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FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY. (U)

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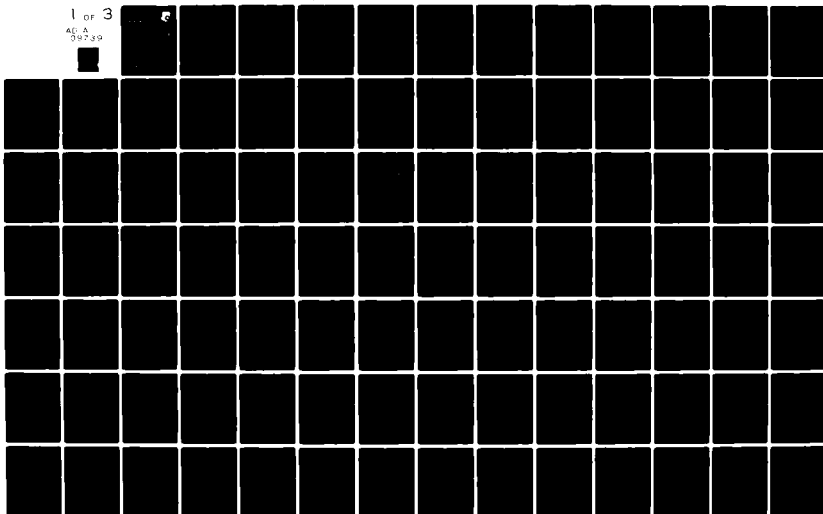
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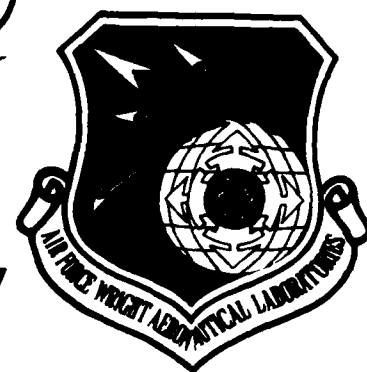
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LEVEL 2



FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY

*McDonnell Douglas Corporation
Douglas Aircraft Company
3855 Lakewood Blvd.
Long Beach, CA 90846*



OCTOBER 1980

TECHNICAL REPORT AFWAL-TR-80-2038
FINAL REPORT FOR PERIOD MAY 1978 THROUGH OCTOBER 1980

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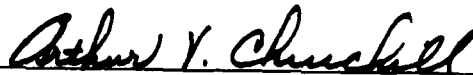
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This technical report has been reviewed and is approved for publication.



CHARLES L. DELANEY
Project Engineer



ARTHUR V. CHURCHILL
Chief, Fuels Branch

FOR THE COMMANDER



ROBERT D. SHERRILL
Chief, Fuels and Lubrication Division

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20. Abstract (Cont'd.)

Increases in availability were predicted for JP-4 of up to 9% and for JP-8 between 41% and 62%. A fuel price analysis was performed based on prices through 1978. Price changes subsequent to the analysis have been so rapid and so unbalanced between products that predictions cannot be made of price changes resulting from specification changes.

PREFACE
ACKNOWLEDGMENTS

Performance of the work on this contract study involved the combined efforts of many individuals in each of the companies involved. While the listed author has been Technical Director of the total effort and is responsible for the summary report of activities, the bulk of the work has been performed by the following people:

Union Science & Technology Division
Union Oil Company of California

H. Hennig, Program Manager
N. C. Burmaster
E. R. Brown
R. M. Matsuo

Pratt & Whitney Aircraft Group,
Government Products Division
United Technologies Corp.

J. R. Herrin, Program Manager
S. A. Mosier
J. R. Grant

McDonnell Douglas Corp.
Douglas Aircraft Co.

J. E. Drever

McDonnell Aircraft

J. J. Snider

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SECTION I INTRODUCTION

In May 1978, Douglas Aircraft Company received a contract (USAF Contract F33615-78-C-2001) from the Air Force to study the effects of broadening the specifications for JP-4 and JP-8 fuel on the performance and cost of all USAF aircraft presently using JP-4 as well as those expected to be introduced into the force structure by 1983.

Phase I of this study was to determine analytically the effects of these specification changes on minimizing fuel cost and maximizing the fuel availability/flexibility without degrading performance, safety, and survivability/vulnerability.

The maximum variations to the properties specifications to be considered were as follows:

<u>Freeze Point, OF (°C)</u>	<u>JP-4</u>	<u>JP-8</u>
Present Specification, max.	-72 (-58)	-58 (-50)
Proposed Variation	+14 (8)	+18 (10)
Proposed Specification, max.	-58 (-50)	-40 (-40)
 <u>Final Boiling Point, OF (°C)</u>		
Present Specification, max.	518 (270)	572 (300)
Proposed Variation	+25 (14)	+25 (14)
Proposed Specification, max.	543 (284)	597 (314)
 <u>Smoke Point, mm</u>		
Present Specification, min		20 ^a
Proposed Variation		-2
Proposed Specification, min		18

^a Maximum 3.0 Volume percent naphthalenes.

Union Oil Company was chosen to study the property variation effects on fuels, Pratt & Whitney Aircraft Group studied the effects on engines, and McDonnell Douglas studied the effects on the airframe. Highlights of these three studies follow. Complete study reports are contained in Appendices to this report.

SECTION II HIGHLIGHTS OF FUEL SUPPLY STUDY

Union Oil Company obtained twenty-four foreign and nine domestic crude assays which contained sufficient data to correlate freeze point and smoke point with initial boiling point and final boiling point. This data was examined to determine the effects on fuel availability, fuel costs, and hydrogen content (an important factor in engine life), when varying the fuel properties to the maximum amount shown in the above table.

When comparing the change from theoretical yields of present specifications to proposed specifications, yields would increase as follows:

	<u>Percentage Increase</u>
JP-4	8.5 - 9.0
JP-8	41 - 62

This large increase in JP-8 is due wholly to the extension of boiling limits in a narrow cut product that are made possible by an extension of the freeze point limits.

Based on wholesale prices published by DOE for the period ending November 1978, price changes which could result from the proposed specification changes and the above volume increases would be as follows, assuming the added volume would come from either diesel or heating oil boiling range stocks:

<u>Using Diesel Prices</u>	<u>Price Change, ¢/gal.</u>	
	Low	High
JP-4	0.30	0.32
JP-8	0.67	1.01
 <u>Using Heating Oil Prices</u>		
JP-4	0.0	0.0
JP-8	-0.81	-1.23

The basic price of JP-4 was 39¢/gal and of JP-8 was 41¢/gal.

The development of this study occurred prior to the publication by DOE of fuel prices for 1979 and prior to much of the middle east conflicts. The product prices analyzed above were taken during a time of relative price stability. The fuel prices published by DOE for 1979 show such a rapid escalation that they cannot be used to predict relative prices between products.

Seven selected crudes were analyzed for changes in hydrogen content which would result from the changes in specifications. Based on a similar weighting system as described for volume effects above, the changes in hydrogen content would be as follows:

	<u>JP-4</u>	<u>JP-8</u>
Hydrogen Contents, wt%		
Proposed Specifications	13.86	13.53
Present Specifications	13.91	13.70
Difference	-0.05	-0.17
Percentage Change	-0.32	-1.20

SECTION III HIGHLIGHTS OF ENGINE STUDY

The overall objective of the engine manufacturer effort in Phase I was to assess the impact of broadened-specification fuels on the performance and durability of gas turbine engines used in USAF aircraft. The various engine related parameters addressed in this phase of the program included ignition characteristics, combustion efficiency, emissions, thermal loads, burner exit temperature distribution, erosion, and coking of the fuel system. The sensitivity of these parameters was discussed with regard to broadened-specification fuels in general, and with regard to the proposed relaxations of current JP-4 and JP-8 fuel specifications shown above.

A fuel characterization study was performed to determine the effects of the proposed changes in JP-4 and JP-8 fuel specifications on fuel hydrogen content. Through the use of interproperty correlations, it was determined that a change from current JP-4 and JP-8 fuel values of final boiling point and smoke point to the proposed specification limits will decrease current fuel values of hydrogen content by 0.25 (% by weight). In addition, changes in other fuel properties, including volatility, specific gravity, viscosity, and thermal stability, implied by the proposed changes in JP-4 and JP-8 fuel specifications were estimated.

A literature survey was conducted to relate the chemical nature and physical properties of fuels to the engine related parameters mentioned previously. The impact of various fuel types on engine performance and durability was qualitatively discussed relative to three USAF engines: the F100, the TF30, and the J57. Whenever possible, estimates as to the extent of this impact were made utilizing the various fuel property changes determined in the fuel characterization study.

Thermal analyses were performed to analytically determine the effect of the proposed relaxations of JP-4 and JP-8 fuel specifications on combustor liner and turbine airfoil temperatures in two USAF engines: the J57-59W and the F100-PW-100. Increases in radiant heat loads to these engine components were found using the estimated change in fuel hydrogen content determined in the fuel characterization study and available data in the literature relating fuel hydrogen content and radiant heat loads. Increases in average liner temperatures resulting from the use of the relaxed specification JP-4 and JP-8 fuels relative to current JP-4 and JP-8 fuels were found to be approximately 90F for the J57 combustor and approximately 60F for the F100 combustor at sea level takeoff operating conditions. Increases in turbine airfoil temperatures were found to be negligible for both engines. The results of the thermal analyses were used to predict the corresponding impact on combustor life for the two engines considered.

The major findings and conclusions of the Phase I effort with respect to durability and performance are as follows:

Durability

The F100 combustor baseline life with current JP-4 and JP-8 fuels will be reduced by approximately 2% (or less) when using the relaxed specification JP-4 and JP-8 fuels.

The J57 combustor is not life-limited to the same extent as the F100 combustor. Individual louvers are repaired and replaced as often as necessary, and a baseline life cannot readily be established. However, as a result of using the relaxed specification fuels relative to current fuels, cracks in the combustor liner will initiate approximately 4.5% (or less) sooner, and have approximately a 2.5% (or less) faster growth rate. In addition, the erosion rate in the vicinity of the crossover tubes may increase by as much as 25%, depending on the exact KC-135 mission profile.

The relaxed specification JP-4 and JP-8 fuels are expected to have no impact on turbine durability in the F100 and J57 engines relative to current JP-4 and JP-8 fuels.

The proposed relaxations of JP-4 and JP-8 fuel specifications are expected to have negligible effect on fuel thermal stability. Therefore, coking of engine fuel systems should not increase.

Performance

The study showed that the proposed relaxed specifications would have a negligible effect on visible smoke emissions.

The relaxed specification JP-4 and JP-8 fuels are expected to have no impact on engine performance, with the exception of ignition capability, relative to current JP-4 and JP-8 fuels. The higher viscosity and lower volatility of the relaxed specification fuels may have an adverse effect on ignition capabilities when fuel and/or air temperatures are relatively low (cold-day ground starts and altitude ignition). The extent of this effect depends on both operating conditions and the particular engine employed, and cannot be predicted due to a lack of pertinent data. However, the incremental effect of the relaxed specification fuels on ignition capabilities relative to current JP-4 and JP-8 fuels is expected to be less than the incremental effect associated with the use of JP-5 relative to JP-4 fuel.

SECTION IV HIGHLIGHTS OF AIRFRAME STUDY

The main objective of the airframe manufacturer effort in Phase I was to determine the effect of broadened - specification fuels on aircraft fuel system performance.

First it was necessary to determine a realistic minimum ambient air temperature envelope to use for all of the airplanes in the study. After reviewing the available ambient air temperature data, it was decided to use the MIL-STD-210B one-day-per year risk minimum temperature profile. All of the airplanes in the study were placed within this temperature envelope during "fuel temperature critical" type missions. The aircraft adiabatic surface temperature (recovery temperature) was then calculated for each mission and this temperature was selected as the limiting case for fuel freeze point considerations.

It was beyond the scope of this program to study all of the airplanes in the Air Force inventory. Several of the Air Force "high fuel user" airplanes were selected for this study following a study of fuel use by aircraft model. Together these aircraft consume 75% or more of the fuel used by the USAF. The airplanes chosen for this study were the KC-10A, C-9, F-4, F-15, B-52, C-130, C-135, C-141 and the C-5.

The fuel systems and fuel management methods of each airplane were studied to evaluate the effect on the system performance of operating with tank fuel temperatures near the freeze point. The recovery temperature was used as a means of predicting minimum inflight fuel temperatures and the relationship with maximum allowable freeze points. It is recognized that the use of MIL-STD-210B and the aircraft recovery temperature is a fairly conservative approach, however, it was decided that a less conservative approach could not be justified with the limited amount of data that is available on this subject.

Using the selected approach, with some consideration of flight conditions, but not considering variation in geographical locations, it was determined that all of the airplanes in the study could obtain tank fuel temperatures below the present maximum allowable freeze point of both JP-4 and JP-8 if operated continuously in a MIL-STD-210B cold temperature environment. If the outside air temperature was considered to vary with flight time and only approached the MIL-STD extremes for limited time, which would be the actual case, the minimum fuel temperatures may not be as low. A standard for variation of temperature with flight time has not been established. It was therefore concluded that the maximum allowable freeze point of JP-4 or JP-8 cannot be increased without degrading system performance and safety as critical conditions are approached.

In actual practice, airplanes today do not fly "worst case" missions which combine high freeze point fuels with persistent low ambient temperatures at the minimum level of the MIL-STD. Therefore, while this study implies that current limits on freeze points may be questionable, the real problem is not current fuel freeze points; rather, the problem is a definition of realistic standards.

SECTION V DISCUSSION OF RESULTS

AVAILABILITY

The study results indicate that a large percentage increase in turbine fuel availability would occur if the specification limits were relaxed. However, the term "availability" is frequently used in a very misleading way.

The meaning of "increased availability" as it is used here must be made clear. A true increase in availability, through specification relaxation, would only occur if the USAF were actually using all the turbine fuel which could be produced within the current specification limits and could get more if the specification limits did not preclude the use of some hydrocarbon product. Relaxing the specification limits would then truly make more fuel available to the USAF. The percentage increases in availability determined in this study are of this variety.

The USAF can get more turbine fuel in time of need by government edict that would direct hydrocarbon supplies falling within the current JP-4 and JP-8 specifications, but now going into gasoline, diesel, and commercial turbine fuel products, into military fuels production.

The USAF is not currently constrained by true availability due to specification limits, i.e., the USAF does not use all the existing hydrocarbon product which fits their specification limits. Rather, limitations are imposed by artificial allocations of product and by the serious desire on the part of the USAF to hold down its expenses for turbine fuel by entering the market in a price competitive manner. Therefore, the result of expanding the fuel specification limits in normal times is to increase "price competitive" availability rather than true availability.

In today's environment, demand exceeds supply. As a nation, we are crude short and refining capacity is strained to the limit. Broadening fuel specifications will not create any more turbine fuel. Increased crude supplies are required to make more total fuel available. Improved refinery processing capability can provide flexibility in meeting market demands and in handling the less desirable crudes which must be used.

Two factors restricting refining capacity are:

- 1) Environmental pressure combined with strict EPA regulations make it very difficult and costly to build a new refinery today.
- 2) New refineries and even new units in existing refineries are harder to justify due to current projections indicating a downturn in gasoline demand over the next decade. Also, it may not be possible to locate assured crude supplies for a new refinery today.

Increased turbine fuel supply must be purchased, therefore, at the expense of decreased gasoline, diesel, or heating oil supply.

PRICE

Study results show that a possible decrease in fuel costs may occur if the variation in allowable JP-8 properties will permit the USAF to participate in the current heating oil market. Price decreases in the range of 0.81 and 1.23¢/gal are

estimated if only the increase in turbine fuel availability comes from the heating oil market. Increased competition in the heating oil market will tend to drive prices up dulling any advantage offered by the specification change. Increased competition will drive the prices up in any market.

The diesel and heating oil product differences are not great. The whole middle distillate market is expected to experience increased competition and upward price pressure without USAF participation in these markets. Since some of this competition is coming from users currently in the gasoline market, the pressure in the gasoline market will lessen, relatively speaking, which will tend to remove some pressure from JP-4 prices.

The USAF is currently in a program to change over from JP-4 to JP-8. The results of this study indicate that a move to JP-8 may increase the USAF costs more than if JP-4 were retained as the primary fuel. Discounting effects on the middle distillate market, study figures, based on prices through 1978, indicate about 2 ¢/gal savings by staying with JP-4.

Staying with JP-4 will remove or significantly reduce any problems of freeze point which may be introduced by extensive use of JP-8 or JP-8 with an increased freeze point. The decreases in durability and engine life and accompanying maintenance cost increases identified in this study would be avoided.

ENGINE EFFECTS

The Union study indicated a hydrogen content change of - 0.17 for JP-8 due to the fuel property changes under study. The Pratt & Whitney study conservatively used a change of - 0.25 in their thermal analysis. The slight effect of even the conservative value of this property change on the engine indicates that the overall effects of the smoke point reduction will be of little significance to USAF operating costs.

ENVIRONMENT

The comparison of MIL-STD-210B one-day-per-year minimum temperatures at cruise altitudes with reported minimum temperatures from commercial aviation experience indicated that the MIL-STD temperatures were in the same vicinity as reported commercial temperatures with a similarly assigned frequency of occurrence. This comparison would indicate that the minimum temperatures at cruise altitudes show enough similarity to reinforce the selection of MIL-STD-210B at cruise altitudes, but does not corroborate the MIL-STD values at other altitudes.

A serious shortcoming of MIL-STD-210B and other atmospheric cold or arctic "day" standards is that no quantitative information is given on the duration of these temperatures in time or location. Analysis of aircraft tank fuel temperatures during a flight requires a profile of temperatures for that flight on the day in question to be of practical use.

A well documented and broadly accepted simple flight model atmosphere into which any aircraft can be placed for analysis is needed. The model would simply be a three dimensional array of static air temperatures covering the areas of the earth's atmosphere where flights are considered.

There have been cursory attempts to define three dimensional models. One model reported by NASA has been used in this report. However, none of these models have been developed to the point of being established as a standard for design. Such a design standard is needed.

A standard model such as just described would allow very definitive analyses of fuel temperatures on particular aircraft to be made that would consider time transients. The relatively conservative approach used in this report of evaluating fuel temperatures on the basis of recovery temperatures and steady state conditions could be improved considerably.

OPERATIONS

The USAF has the choice to (1) provide fuel with a freeze point low enough to satisfy every aircraft on every mission or to (2) allow a slightly higher freeze point fuel and accept the possibility of deviation from intended flight path on those occasions when air temperatures and mission profiles result in fuel freeze point encounters.

Inflight deviations can be minimized by two methods:

1. Considering that aircraft are flown to preselected fuel temperature/fuel freeze point margin limits where these margins are set by the aircraft manufacturer or by the USAF as a result of analysis and testing programs, fuel temperature margin requirements can be reduced by improvements of inflight fuel temperature measuring or possibly by fuel system modifications.
2. Actual fuel freeze points are usually below the maximums allowed by specification. The development and deployment of a field capability to measure actual fuel freeze points will prevent unnecessary flight deviations due to using specification limits in flight crew instructions.

AIRCRAFT DESIGN

The way fuel is held, transferred, and used in an aircraft is known as its fuel management schedule. Specific components or fuel subsystems may be installed to accomplish this task. The combination of the fuel management schedule and the fuel tankage arrangement of an aircraft has a very significant effect on the minimum fuel temperature experienced on a given flight.

Study of the various aircraft considered in this effort indicates that there are many different fuel management systems in use. Fuel management can be quite complicated on a large multi-engine aircraft. The scheme can vary with aircraft model and with specific missions.

Fuel management schemes have been primarily selected to provide advantages in the areas of center-of-gravity (c.g.) control and structural load control. The ultimate benefit is a more efficient airplane.

Aircraft center of gravity shift can be controlled by moving fuel among the tanks or by using fuel from the tanks to the engines in a prescribed manner. These fuel management techniques may be applied during aircraft loading to either allow more variation in where payload can be located aboard the aircraft or to accommodate specific cargo loadings where weight may be concentrated at a specific location.

Structural load control is usually practiced by maintaining fuel outboard in the wings to counter aerodynamic loads. Load alleviation by counter-acting fuel load forces results in a lighter airframe structural requirement than if the aerodynamic loads were handled by pure structural strength. Wing flutter damping by maintaining fuel mass outboard also results in reduced structural weight of the airframe. Reduced airframe weight can allow greater payloads or can give greater fuel efficiency to the airplane.

Combinations of c.g. control and structural load control can be used in other ways to reduce airplane weight and drag. These concepts are "built in" to the airplane to make it the most efficient package possible.

Deviations from normal fuel management schedules to maintain fuel temperatures at higher levels would have to be evaluated for each specific aircraft to determine what other limitations may result. It may occur that such deviations would result in greater fuel consumption which would be counter productive to the original intent of increasing fuel availability and reducing costs by allowable fuel freeze point increases.

The allowable limit of congealed fuel buildup in fuel tanks at low temperatures is not defined. Other fuels/fuel system research efforts are contemplated or are under way by NASA and the USAF which will contribute toward understanding this limit. The amount of congealed fuel cannot be measured directly and displayed in the cockpit. A correlation of unavailable fuel at the critical point in a flight with a parameter such as fuel temperature at a selected point is needed for each airplane and system if this complicated process is to be allowed to progress to where a system is pushed to its limit. While a thorough understanding of the phenomena which occur when operating in this area is required, the state-of-the-art of handling conditions this close to system failure is not sufficiently developed to consider normal operations with fuel in this physical state.

Future design/procurement requests for specific aircraft should include studies to evaluate the trade offs involved in designing the vehicle to accommodate fuel of an increased freeze point. These studies should be directed at tank fuel heating schemes, insulation concepts, constructions that are less inhibiting to flow of semi-solid fuel, and alternative fuel management concepts. It is essential that a design temperature environment that is realistic be established to provide a common reference for these studies.

SECTION VI CONCLUSIONS

- The specification maximum allowable freeze point of JP-4 or JP-8 cannot be increased without degrading aircraft system performance and safety on the basis of criteria available to this study.
- There will be no impact on engine performance, turbine durability, or coking.
- There will be a small increase in maintenance costs due to a less than 2 to 4% decrease in combustor life.
- Using JP-4 as the standard USAF fuel will avoid the use of high-demand middle-distillate fuels and will give producers flexibility.
- Extensive use of JP-8 in the United States will increase middle-distillate demand and will cause a slight increase in hot section maintenance.
- A realistic environmental envelope for aircraft operations is the most important overall factor in appraisals of the adequacy of fuel freeze points. A broadly accepted single flight model atmosphere is needed to improve the evaluation of limiting conditions on aircraft fuel temperatures.
- The USAF may increase fuel freeze points in peace time by being willing to examine flight profiles, actual fuel freeze points, and upper air temperatures on critical flights prior to dispatch and by planning for diversions or flight cancellations as a method of coping with low fuel temperatures.
- The proposed increases in freeze point, smoke point and end point will have no effect on survivability/vulnerability.
- Future design/procurement requests for specific aircraft should include studies to evaluate the tradeoffs involved in designing the vehicle to accommodate fuel of an increased freeze point.

SECTION VII RECOMMENDATIONS

- The data base from which availability and fuel properties information was obtained should be improved and updated to improve the results.
- A more accurate determination of the actual crude sources from which turbine fuel is actually derived should be made to improve study results. The current study was made on an overall crude property basis.
- Testing to verify and improve engine and airframe effects should use specially blended test fuels having properties meeting the limits of a relaxed specification.
- Experimental programs should be conducted to determine the impact of fuel properties on engines. Particularly on:
 - a. The effects on carbon particulate formation, deposition and erosion tendencies.
 - b. Fuel thermal stability and its relation to coke formation in actual engine fuel systems.
 - c. The effects on augmental performance and durability.
- Programs directed towards developing improved durability combustor liner designs should be conducted both for new design and retrofit applications.
- The USAF should define a cold day single flight model atmosphere for use in design and evaluation of aircraft fuel systems performance.
- A survey of aircraft at specific bases and their missions should be made to evaluate the ability to convert the base fuel supply to higher freeze point fuels.
- The USAF should consider the use of actual fuel freeze points rather than specification maximums for aircraft dispatch evaluations on non-critical missions to ease the impact of any move to higher allowable freeze points.
- All aircraft in the inventory which are to be operated close to fuel freeze points should have a review of their systems & procedures for operating nearer to actual fuel freeze points.
- A test procedure guideline and a test fuel should be developed to enable manufacturers to run meaningful tests for evaluation of their systems & the development of instrumentation system improvements.
- Future aircraft design requirements should recognize the desire to make systems less sensitive to low temperature operations. Fuel tankage and fuel management systems of future aircraft should be evaluated for the trade offs between designing to handle higher freeze point fuels and any fuel cost savings to be realized.

APPENDIX A

FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY
(FUEL STUDY)

June 15, 1979

By

N. C. Burmaster

H. Hennig, Program Manager

Literature Search
Conducted By:

E. R. Brown
R. M. Matsuo

UNION SCIENCE & TECHNOLOGY DIVISION
UNION OIL COMPANY OF CALIFORNIA

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SECTION 1.

INTRODUCTION AND SUMMARY

A. Introduction

The United States Air Force¹ issued a Request for Proposal on November 19, 1977, for the purpose of studying the effects of changing certain specifications in turbine fuels JP-4 and JP-8. Douglas Aircraft Company, as prime contractor, issued a Technical Proposal titled "Fuel/Engine/Airframe Trade-Off Study" on January 9, 1978².

Pratt and Whitney Aircraft Group is subcontractor for engines; Union Oil Company, Science and Technology Division, is subcontractor for fuels. Authority to proceed was received on 11 July 1978 from Douglas. This Report covers Phase I of the authorized study program.

The main objective of the fuels portion of the program is to determine the effect of changing specifications on the availability of aviation turbine fuels. Other objectives of the study are to determine the effect of the same specification changes on fuel price and hydrogen content (an important factor in engine life).

An earlier study by Bonner & Moore³ attempted to determine jet fuel availability changes due to relaxed specifications by using a poll of petroleum refining companies representing 21% of the U. S. jet fuel production.

Bonner and Moore's study has the weakness of relying on a respondent's subjective answers rather than the physical properties of the petroleum sources of the jet fuel. Therefore, the present study was based on correlating data from crude assays and utilizing data available from literature sources.

B. Summary

For Phase I, maximum variations to the following properties specifications as given in the Technical Proposal 77D-357T page 4, are considered:

	<u>JP-4</u>	<u>JP-8</u>
<u>Freeze Point, °F</u>		
Present Specification, max.	-72	-58
Proposed Variation	+14	+18
Proposed Specification, max.	-58	-40
<u>Final Boiling Point, °F</u>		
Present Specifications, max.	518	572
Proposed Variation	+25	+25
Proposed Specification, max.	543	597

- 1 United States Air Force, Air Force Systems Command, Aeronautical Systems Division/PMRSA, Wright Patterson AFB, Ohio 45433, RFP No. F33615-78-R-2001, Dated 19, November 1977.
- 2 Proposal 77D-357T, 9 January 1978, Douglas Aircraft Company, 3855 Lakewood Boulevard, Long Beach, California 90846. (Proprietary Publication)
- 3 Impact of Fuel Properties on Jet Fuel Availability, April 1975-April 1976, Contract No. F33615-75-C-2022, Bonner and Moore Associates, Inc., 500 Jefferson, Houston, Texas 77002.

	JP-8
<u>Smoke Point, mm</u>	
Present Specification, min.	20 ^a
Proposed Variation	-2
Proposed Specification, min.	<u>18</u>

a Maximum 3.0 Volume percent naphthalenes.

Twenty-four foreign and nine domestic crude assays which contained sufficient data to correlate these properties were obtained. Plots of smoke point and freezing point were made against initial boiling point (IBP) and/or final boiling point (FBP). These plots were used to estimate limits of FBP for freezing point and smoke point.

After determining which variable was limiting on the particular crude, the increases in volume yields were determined from the crude assay for present and proposed specifications.

U. S. crude oil consumption for foreign crudes by country source and domestic crudes by state source were obtained from DOE publications. The crude assays analyzed were assumed to represent the respective country or state on a volume basis, as a method to average the results.

When comparing the change from theoretical yields of present specifications to proposed specifications, yields would increase as follows:

	<u>Percentage Increase</u>
JP-4	8.5 - 9.0
JP-8	41 - 62

Based on wholesale prices published by DOE, price changes which could result from the proposed specification changes and the above volume increases would be as follows, assuming the added volume would come from either diesel or heating oil boiling range stocks:

	<u>Price Change, ¢/gal.</u>	
<u>Using Diesel Prices</u>	<u>Low</u>	<u>High</u>
JP-4	0.30	0.32
JP-8	0.67	1.01
<u>Using Heating Oil Prices</u>		
JP-4	0.0	0.0
JP-8	-0.81	-1.23

Seven selected crudes were analyzed for changes in hydrogen content which would result from the changes in specifications. Based on a similar weighting system as described for volume effects above, the changes in hydrogen content would be as follows:

Hydrogen Contents, wt%	JP-4	JP-8
Proposed Specifications	<u>13.86</u>	<u>13.53</u>
Present Specifications	<u>13.91</u>	<u>13.70</u>
Difference	-0.05	-0.17
Percentage Change	-0.32	-1.20

4 Weighted average of seven crudes studied, 140°F IBP cut on JP-4.

SECTION 2.

SCOPE OF STUDY

Three main fuel supplier tasks to be performed by the fuel supplier for Phase I were outlined on p.34 of the technical proposal for this study. These tasks are outlined below:

- Task 1 - Survey the literature for data required in this program.
- Task 2 - Determine the increase in availability of turbine fuel based on the best information from Task 1.
- Task 3 - Estimate the change in cost for changes in specifications of the turbine fuels.

The following turbine fuel properties and maximum variation from current specifications were to be considered:

<u>Property</u>	<u>JP-4</u>	<u>JP-8</u>
Freezing Point	+140°F	+180°F
Final Boiling Point	+250°F	+250°F
Smoke Point	-	-2mm

The following Table contains the current specifications and the proposed specifications which would result from the above assumed maximum variation to the current specification:

	<u>Specifications</u>	
	<u>Current</u>	<u>Proposed</u>
<u>JP-4</u>		
Freezing Point, °F	-72	-58
Final Boiling Point, °F	518	543
<u>JP-8</u>		
Freezing Point, °F	-58	-40
Final Boiling Point, °F	572	597
Smoke Point, mm	20 ^a	18 ^a

a With 3 vol % naphthalenes (max).

The Bartlesville Energy Research Center (BERC), DOE, Bartlesville, Oklahoma publishes an annual aviation turbine fuels survey. Table 2.1 lists the minimum, maximum, and average initial boiling points and final boiling points for JP-4, JP-5, and JET-A for 1977, as listed by BERC.

JP-4 must have a vapor pressure of 2.0 to 3.0 psia at 100°F under current specifications. By plotting vapor pressure of naphtha cuts against IBP, we find that an IBP of 100° will usually satisfy the vapor pressure requirement. The average IBP of JP-4 in 1977 was 138°F which suggests that, in most cases, butanes are probably added to satisfy vapor pressure specifications. Therefore, both 100°F and 140°F initial boiling points were analyzed for JP-4 in this study.

JP-8 has a minimum flash point specification instead of a vapor pressure specification. An IBP of 300°F was assumed to satisfy the flash point requirement and was used in the study for JP-8.

TABLE 2.1

AVIATION TURBINE FUELS, 1977BARTLESVILLE ENERGY RESEARCH CENTER
BARTLESVILLE, OKLAHOMA

	<u>MIN</u>	<u>MAX</u>	<u>AVG</u>
JP-4 (28 Samples)			
IBP	117	173	138
FBP	370	503	450
JP-5 (7 Samples)			
IBP	264	378	351
FBP	470	566	504
JET A (65 Samples)			
IBP	300	378	337
FBP	470	545	513

The average FBP for JP-4 in 1977 was reported to be 450°F. This is much lower than a FBP designed to meet freezing point or smoke point specifications. A FBP of 450°F was selected to represent "present yields" of JP-4 in this study.

However, since JP-4 has a relatively small part of the total petroleum market, it is now felt that short of a wartime emergency situation, comparing changes in specifications to this "present yield" is not a realistic approach.

SECTION 3.LITERATURE SURVEY

Six literature search services were utilized to obtain pertinent data for this program. In addition, a hand search was made of industry trade journals for the most recent issues which would not be included in the computer data banks.

Literature services used were:

1. CA - Chemical Abstracts.
2. EL - Energy Line (Environment Information Center)
3. FR - Federal Register.
4. NTIS - National Technical Information Service.
5. SSIE - Smithsonian Science Information Exchange.
6. API - American Petroleum Institute.

The following table lists the various keyword combinations used in each search:

<u>Source</u>	<u>Years</u>	<u>Jet Fuel</u>	<u>Jet Fuel Supply</u>	<u>Jet Fuel Properties</u>	<u>Jet Fuel Costs</u>	<u>Jet Fuel Handling</u>	<u>Jet Fuel & Crude Oil</u>	<u>Jet Fuel & Specs or Quality</u>
CA	1970-78			X	X	X	X	X
EL	1971-78		X	X	X	X	X	
FR	1977-78	X						
NTIS	1970-78		X	X	X		X	
SSIE	1974-78		X		X			
API	1964-77	Crude Oil & Jet Fuel Jet Fuel & Specifications/Product Quality & Cost Jet Fuel & Specification/Product Quality & Material Handling/ Tank/Filter/Filtration Jet Fuel & Specification/Product Quality & Melting Point/ Final B.P./Smoke Point/Viscosity/Density/Aromatics/ Hydrogen Jet Fuel & Specifications/Product Quality & Supply						

SECTION 4.

CRUDE ASSAYS

A Major Sources of Crude

Major sources of crude oil utilized in the United States had to be identified in order to determine which crude assays would be useful.

Figure 4.1 displays plots of domestic, foreign, and total crude runs in U. S. oil refineries for the years 1950 through 1978. Here we see the dramatic rise in foreign oil imports accompanied by a decline in domestic oil production which has resulted from political and economic forces since the late 1960's. Figure 4.1 shows that foreign crudes must be analyzed since they constitute such a large percentage of the U. S. crude consumption.

Figure 4.2 shows the production of major refined distillate products in the U. S. for the same period. Jet fuel has increased about 1.2% per year over the ten year period 1968-1978. Distillate fuel oil and gasoline have increased 3.7% and 4.0% per year for the same period. Distillate fuel oil includes diesel, home heating oil, and other light fuel oils. JP-8 must compete with distillate fuel oil for volume and proposed changes in endpoint would come out of the distillate fuel oil portions of the barrel.

JP-4 competes mainly with gasoline, although historically, mainly low octane stocks which were not suitable for gasoline were used in JP-4. With the proposed new specifications, the extra volume for JP-4 would come from distillate fuel oil boiling range stocks.

Seven States provide 87% of the domestic crude produced in the U. S. These are Texas, Louisiana, Alaska, California, Oklahoma, Wyoming, and New Mexico; listed in order of volume produced in 1978. Figure 4.3 shows the cumulative volume percents of crude these states have produced since 1950.

Figure 4.4 is similar to Figure 4.3 except that it deals with major foreign oil imports. Prior to 1973, Venezuela and Canada were the two largest oil importers. Since 1973, Arabian countries have been the largest importers followed by Indonesia and Nigeria. Recent events in Iran will change the relationship of these curves even more.

B LOCATING CRUDE ASSAYS

Having identified the domestic oil producing states and the foreign oil exporting countries important to the U. S. oil supply, the selection of crude assays could begin.

Crude assays vary in style and content with each company developing them and with need and cost justification for their development. Most United States oil companies produce relatively simple crude assays. This may be due to the fact that the crude oils are either consumed by the oil company or often sold on long term contracts.

International oil companies, which have traditionally sold crudes on a world wide market, have developed very elaborate crude assays. This has often carried on into domestic crudes they produce, since these same companies may have a wide variety of refinery combinations to operate.

FIGURE 4.1
DOMESTIC AND FOREIGN CRUDE RUN TO STILL IN THE U.S.

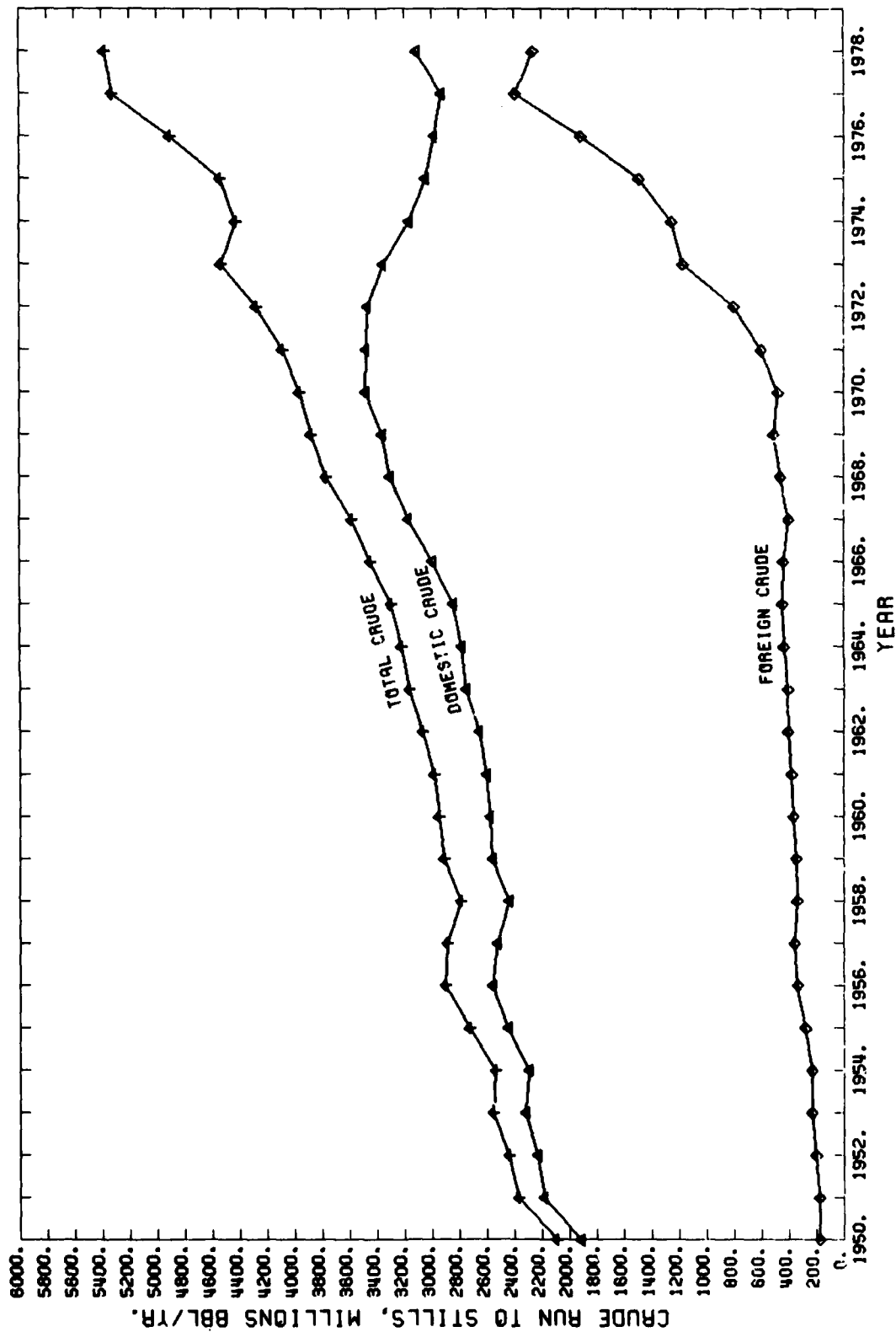


FIGURE 4.2
PRODUCTION OF REFINED PRODUCTS IN THE UNITED STATES.

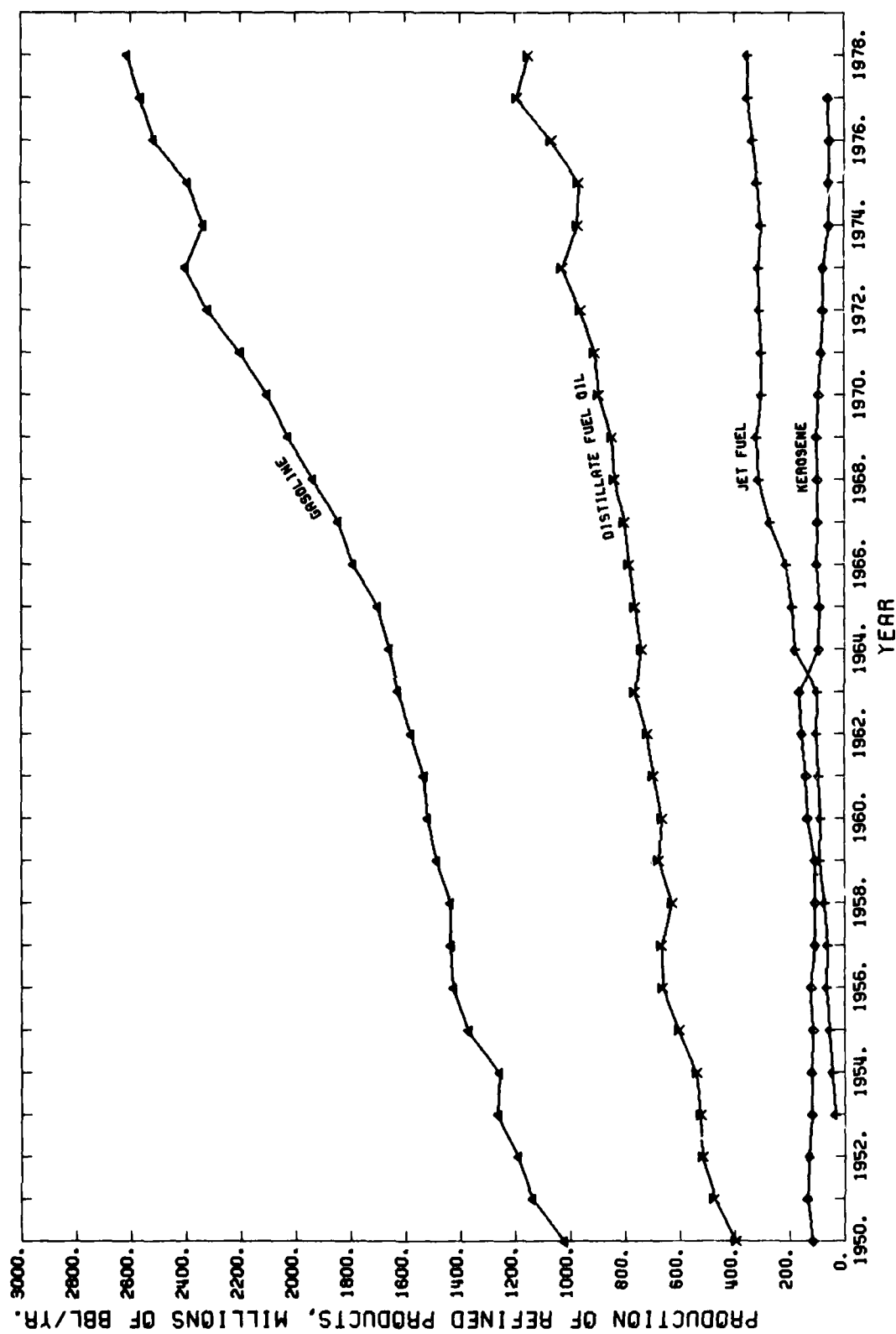


FIGURE 4.3
CUMULATIVE PERCENT OF CRUDE OIL
PRODUCED BY SEVEN MAJOR STATES

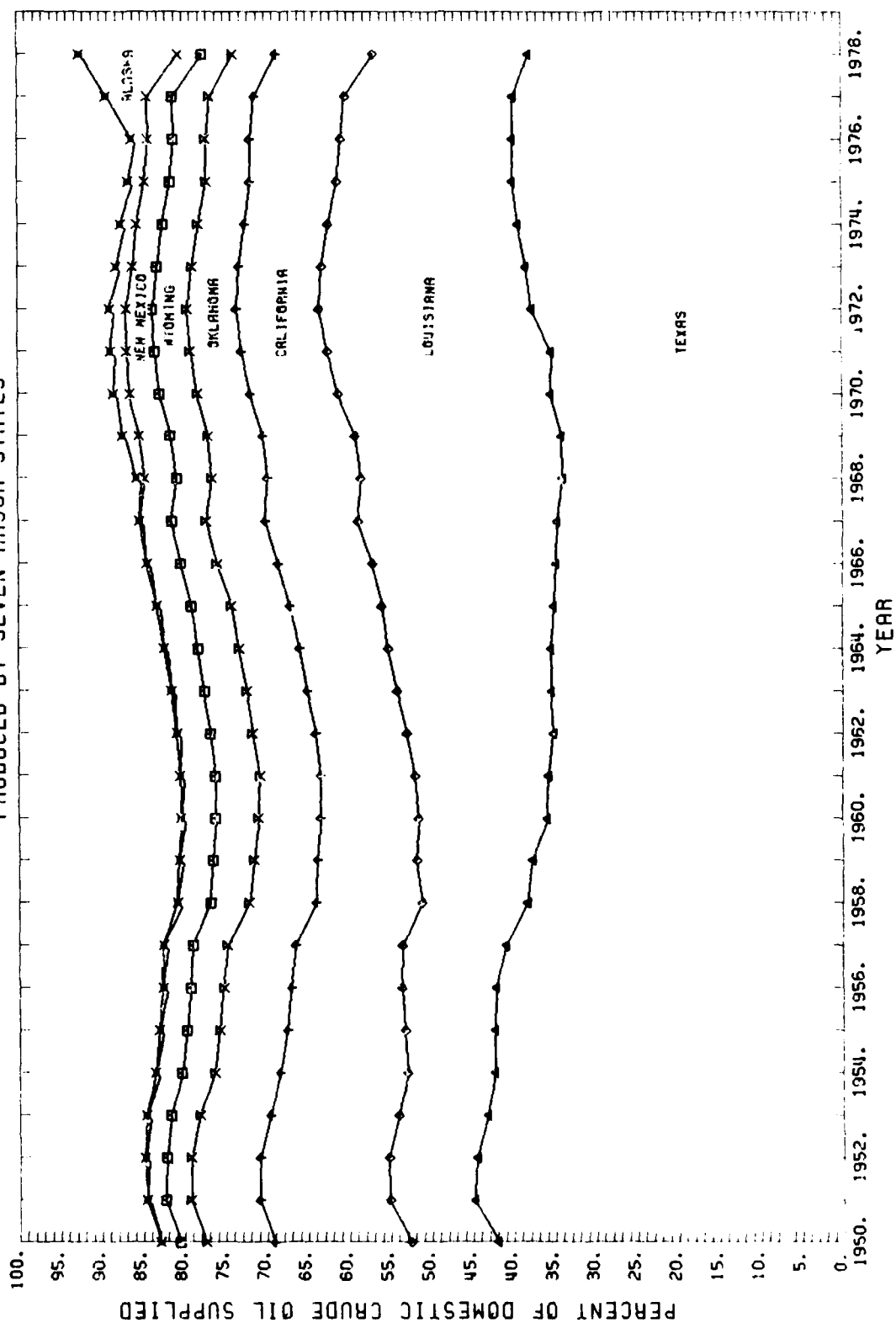
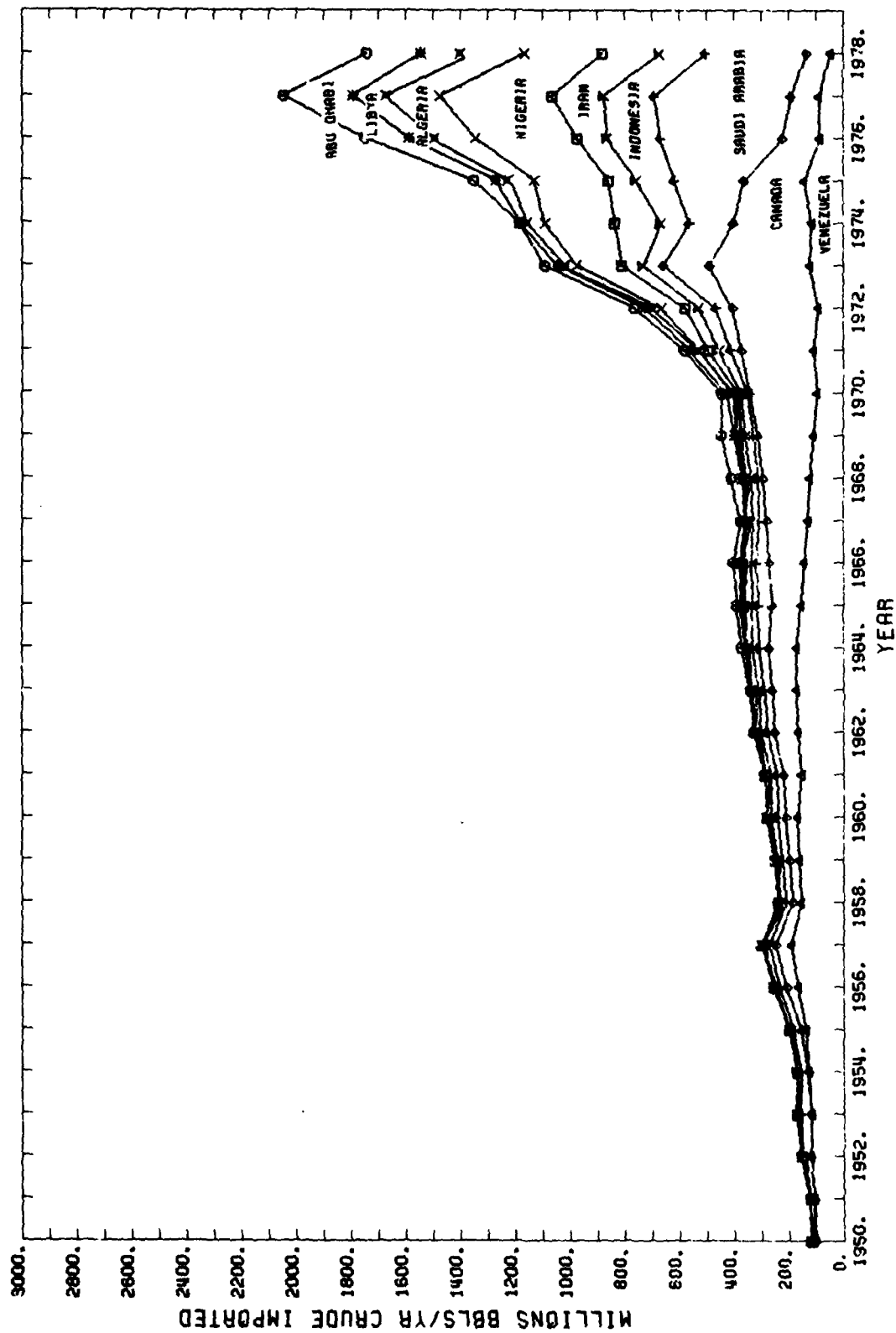


FIGURE 4.4
CUMULATIVE CRUDE IMPORTS FROM MAJOR FOREIGN COUNTRIES.



A crude assay is made by distilling a sample of crude petroleum into a series of progressively higher boiling "cuts" in a batch distillation column. These cuts may be very narrow, such as every 2°F, or very wide, simulating the boiling range of the final products produced by the refinery. If the column overhead temperature for each cut point is plotted against volume percent distilled, this is referred to as a distillation curve. If narrow cuts are obtained from a column operated to provide precise separation of cuts, the distillation curve is called a "true boiling point" (TBP) curve.

The main requirement of crude assays which could be used in this study was to have a number of jet fuel type distillate cuts from a given crude with smoke point and freezing point analysis so that these properties could be extended to any distillate cut point desired.

Twenty-four foreign and nine domestic crude assays were obtained which met the above requirement. About fifteen foreign crude assays were obtained from the files of the Union 76 Overseas Division of Union Oil Company. These included Middle Eastern, African, and Indonesian crudes.

Jet fuel data on six domestic crudes were obtained from a large international oil company. These are of the same format as their foreign crude assays and contain multiple jet fuel cuts.

Twenty-two miscellaneous crude assays were obtained from a large domestic oil company. Not all of these were usable for this study. Crudes used in this study are listed in Table 4.1.

Table 4.1

Crude Assays Used in Study

Domestic Crudes

- 1 Prudhoe Bay, Alaska (North Slope)
- 2 East Wilmington, California (Long Beach Area)
- 3 Midway Sunset, California (San Joaquin Valley)
- 4 Ventura, California
- 5 West Delta Block, Louisiana (Offshore)
- 6 Rocky Mountain Sour
- 7 Hawkins Mix, Texas (District 06)
- 8 East Texas
- 9 West Texas

Foreign CrudesMideast

- 1 Arabian Light
- 2 Ardishir, Iran
- 3 Basrah, Iraq^a
- 4 Basrah, Iraq
- 5 Dubai
- 6 Iranian Light
- 7 Kafji, Neutral Zone
- 8 Kirkuk, Iraq
- 9 Kuwait
- 10 Murban, Abu Dhabi
- 11 Oman
- 12 Rostam, Iran

African

- 13 Brega, Libya
- 14 Brut Mandji, Gabon
- 15 Es Sider, Libya
- 16 Hassi Messaoud, Algeria
- 17 Nigerian Light
- 18 Zueitina, Libya

South America

- 19 Tia Juana Medium, Venezuela

Southeast Asia

- 20 Seria, Indonesia
- 21 Sumatran Light, Indonesia
- 22 Tapis #3, Malaysia

North Sea

- 23 Tiber

Canada

- 24 Pembina

^a Basrah assays were run from the different sources.

SECTION 5.

DEVELOPMENT OF TURBINE FUEL YIELDS

Having crude assay data, the first requirement was to determine how well smoke point and freezing point correlate with cut points. Figures 5.1, 5.2, 5.3, and 5.4 are plots of these data for various middle Eastern and African crudes. Freezing Point and Smoke points are different for each crude due to the variation in the composition of crude. Freezing points plot as reasonably straight lines for most crudes although the slopes are different. Smoke points appear to be more erratic, but one must remember that the smoke point test is not perfectly reproducible and that smoke points are reported to the nearest whole number (i.e.: 20 or 21, not 21.5). Therefore, one cannot expect the data to plot perfectly linearly.

A number of steps are involved in developing turbine fuel yields from a crude assay to match specific freezing point or smoke point specifications. The remainder of this section will attempt to walk the reader through the procedure for determining freezing point limits. The same procedure would be used for smoke point. A Kuwait crude assay is used to illustrate the procedures.

The turbine fuel cuts obtained from the Kuwait crude assay are tabulated in Table 5.1. The cuts are listed according to "true boiling point" (TBP) initial and final cut points. Yields, smoke points and freezing points are shown for each cut.

Ten cuts are listed in the Kuwait assay. Five cuts have a common 302°F initial boiling point (IBP) with various final boiling points (FBP). The other five cuts have IBP's which match FBP's of the 302°F IBP cuts.

The freezing point data for Kuwait crude (as given in Table 5.1) is first plotted against initial boiling point with parameters of FBP. (For some crudes, it is more convenient to plot against FBP with parameters of IBP). These curves are shown in Figure 5.5.

Next, lines of constant freezing point are drawn on the graph for -40, -58, and -72°F, to represent the various present and proposed freezing point limits. Initial boiling points are read for the intersection of the constant FBP lines and the constant freezing point lines.

Utilizing the IBP's and FBP's obtained above for each freezing point, curves of constant freezing point are plotted using FBP on the abscissa and IBP on the ordinate as shown in Figure 5.6. This curve allows one to determine the limiting FBP for any given IBP (or vice versa) for each of the three freezing points used in the study.

The six large dots on Figure 5.6 indicate the combination of IBP and FBP that satisfy either freezing point or FBP limits for the JP-4 and JP-8 fuels. The two dots labeled JP-4 (PRESENT) represent present specifications for JP-4 with either 100°F or 140°F IBP. The 140°F IBP JP-4 (PRESENT) is limited by both the -72°F freezing point curve and the 518°F FBP limit. The 100°F IBP JP-4 (PRESENT) is limited by the 518°F FBP limit but not the freezing point.

Proposed specification JP-4 fuels (both 100°F and 140°F IBP) are limited by the 543°F (MAX) FBP specification, but not in the -58°F freezing point specifications. These are represented by the large dots labeled JP-4 (Proposed).

FIGURE 5.1
JET FUEL FREEZING POINT VS FBP FOR MID EAST CRUDES

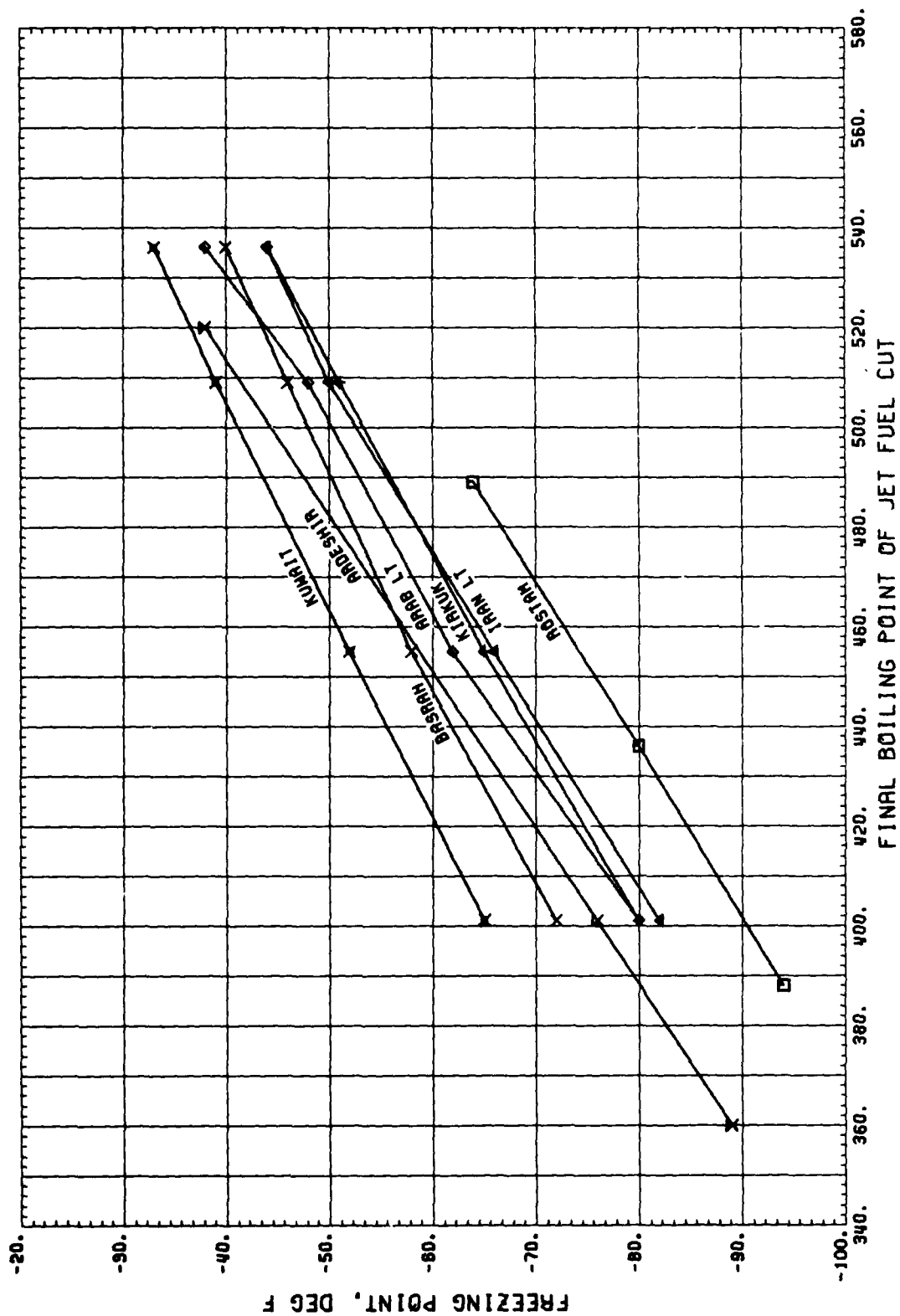


FIGURE 5.2
JET FUEL SMOKE POINT VS FBP FOR MID EAST CRUDES

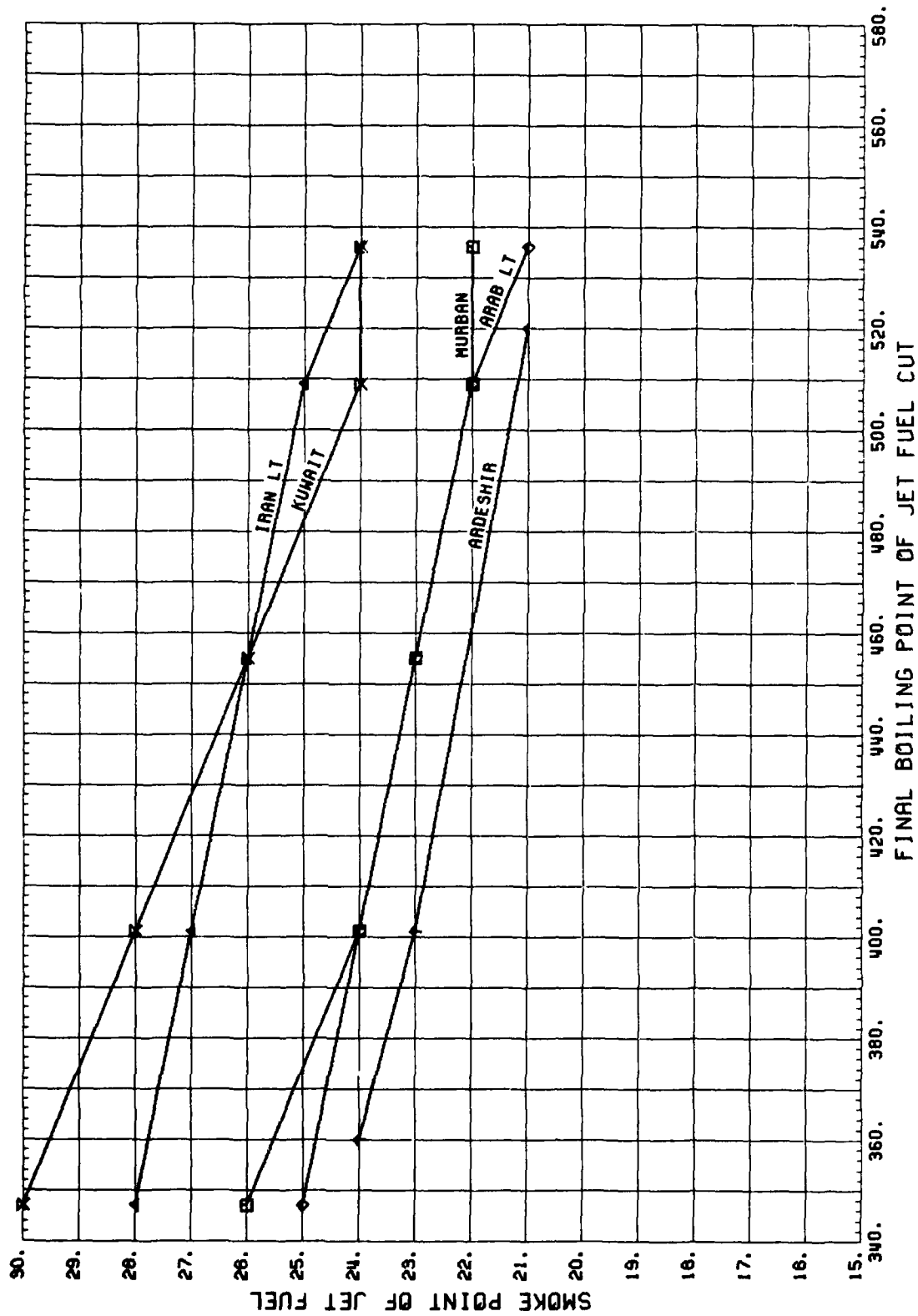


FIGURE 5.3
JET FUEL FREEZING POINT VS FBP FOR AFRICAN AND MISC. CRUDES

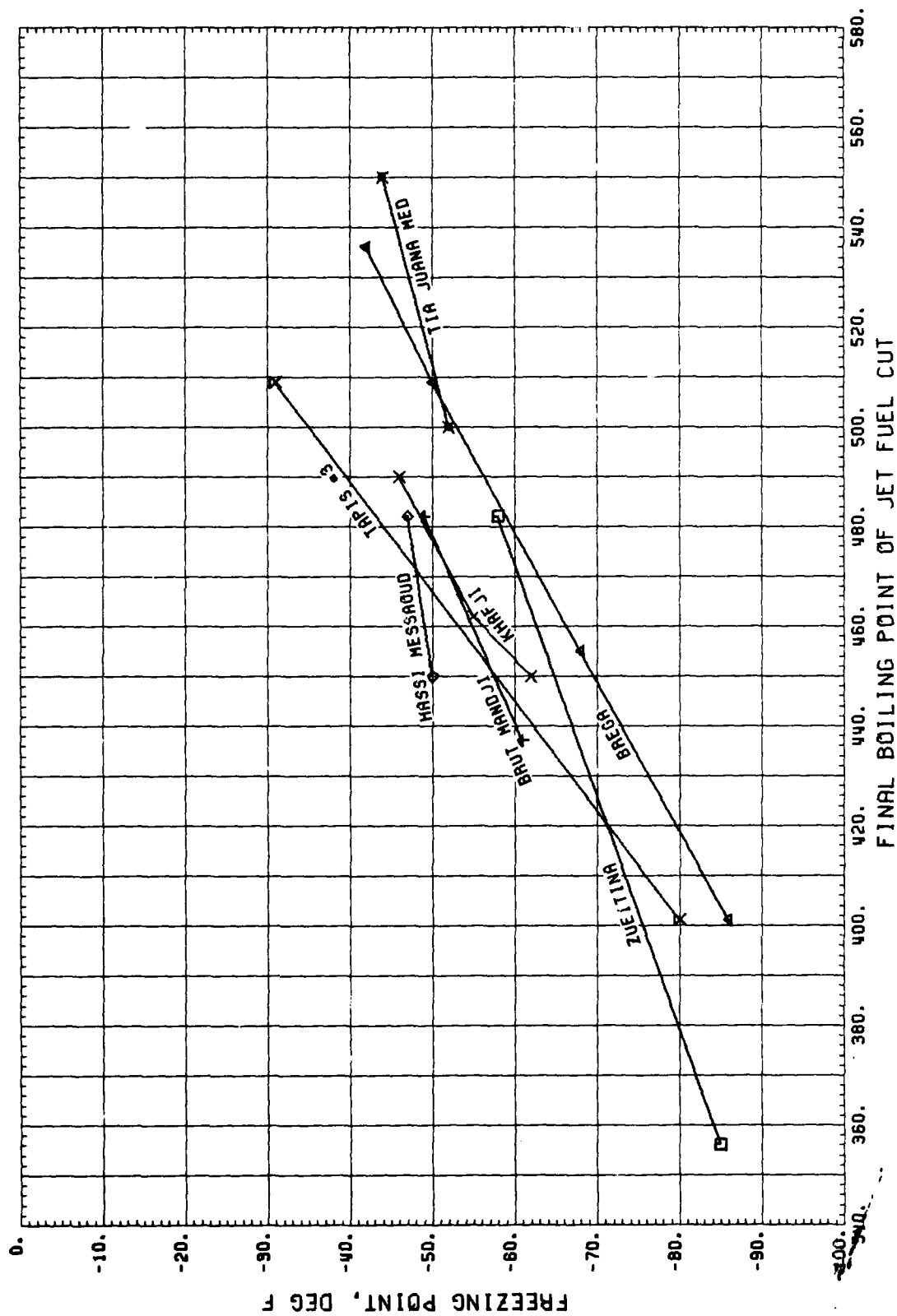


FIGURE 5.4

JET FUEL SMOKE POINT VS FBP FOR AFRICAN AND MISC. CRUDES

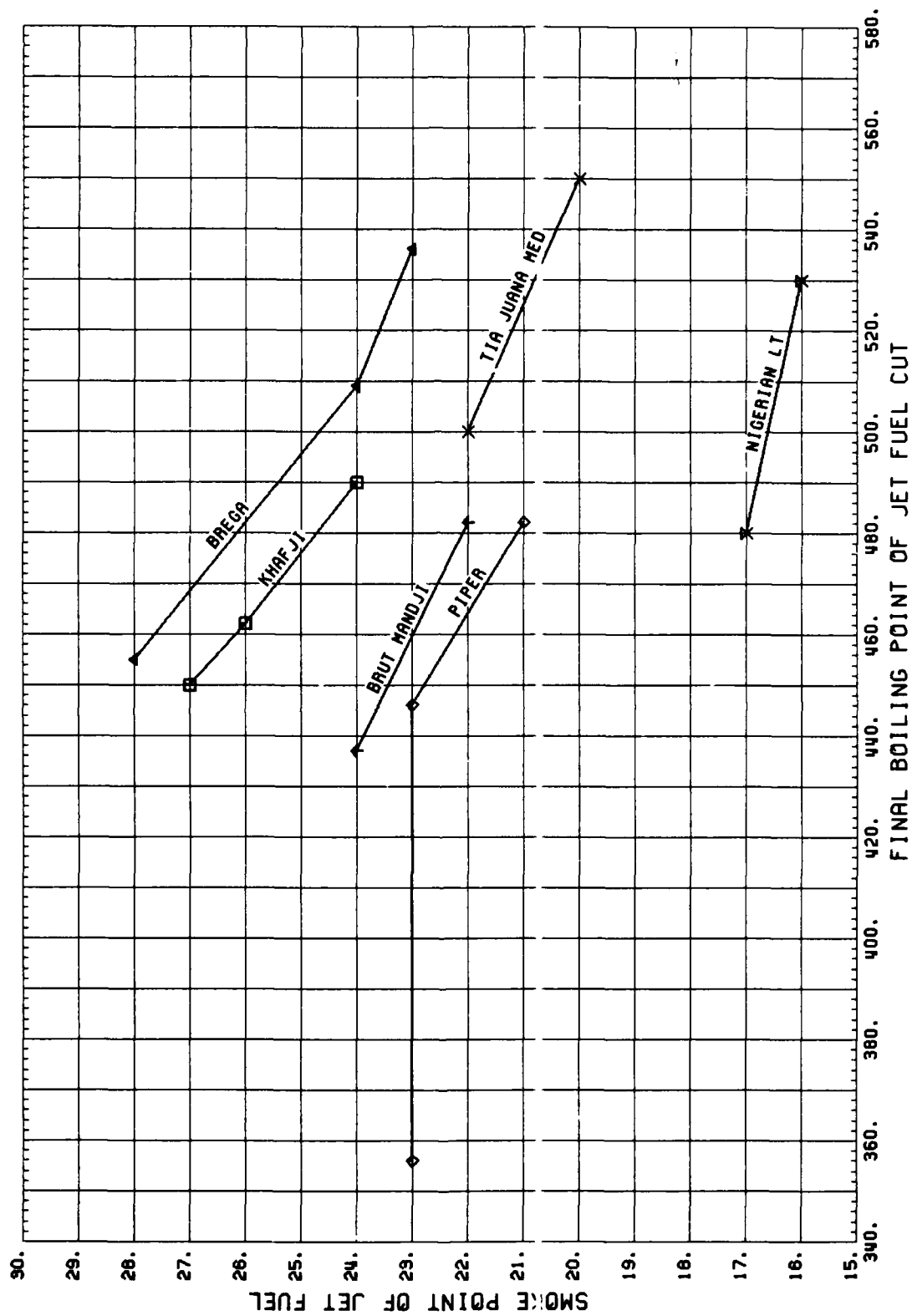


Table 5.1

KUWAIT CRUDE ASSAY
(Selected Turbine Fuel Data)

<u>TBP CUT</u> <u>POINT, °F a</u>	<u>YIELD CUT</u> <u>RANGE, VOL %</u>	<u>SMOKE POINT,</u> <u>MM</u>	<u>FREEZING</u> <u>POINT, °F</u>
302-347	19.9-23.8	30	-
302-401	19.9-28.4	28	-65
302-455	19.9-33.2	26	-52
302-509	19.9-37.9	24	-39
302-536	19.9-40.4	24	-33
347-401	23.8-28.4	26	-54
401-455	28.4-33.2	23	-30
455-509	33.2-37.9	21	-5
374-536	26.2-40.4	22	-16
509-536	37.9-40.4	20	+11

a IBP to FBP.

FIGURE 5.5
KUWAIT JET FUEL - FREEZING POINT VS INITIAL BOILING POINT

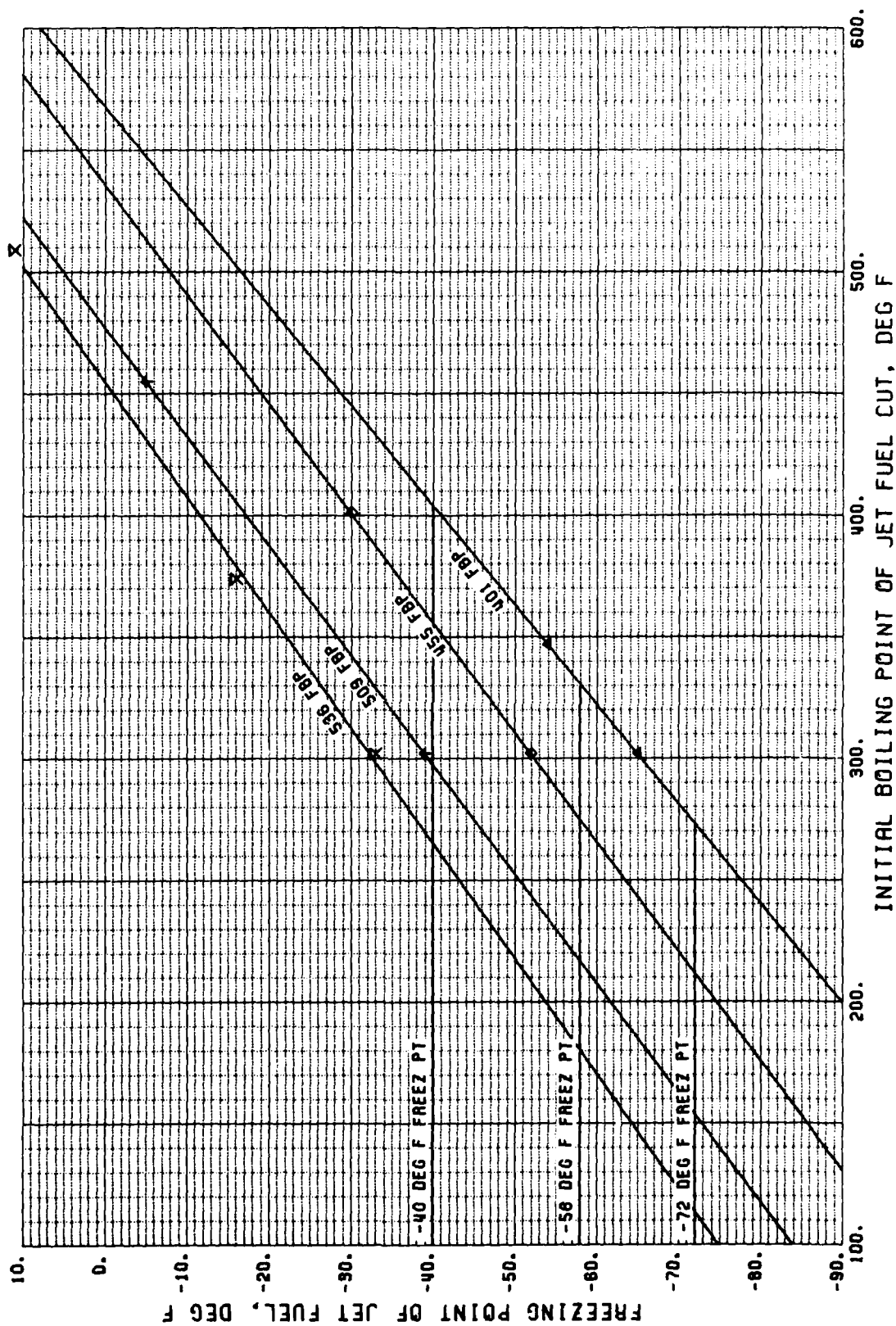
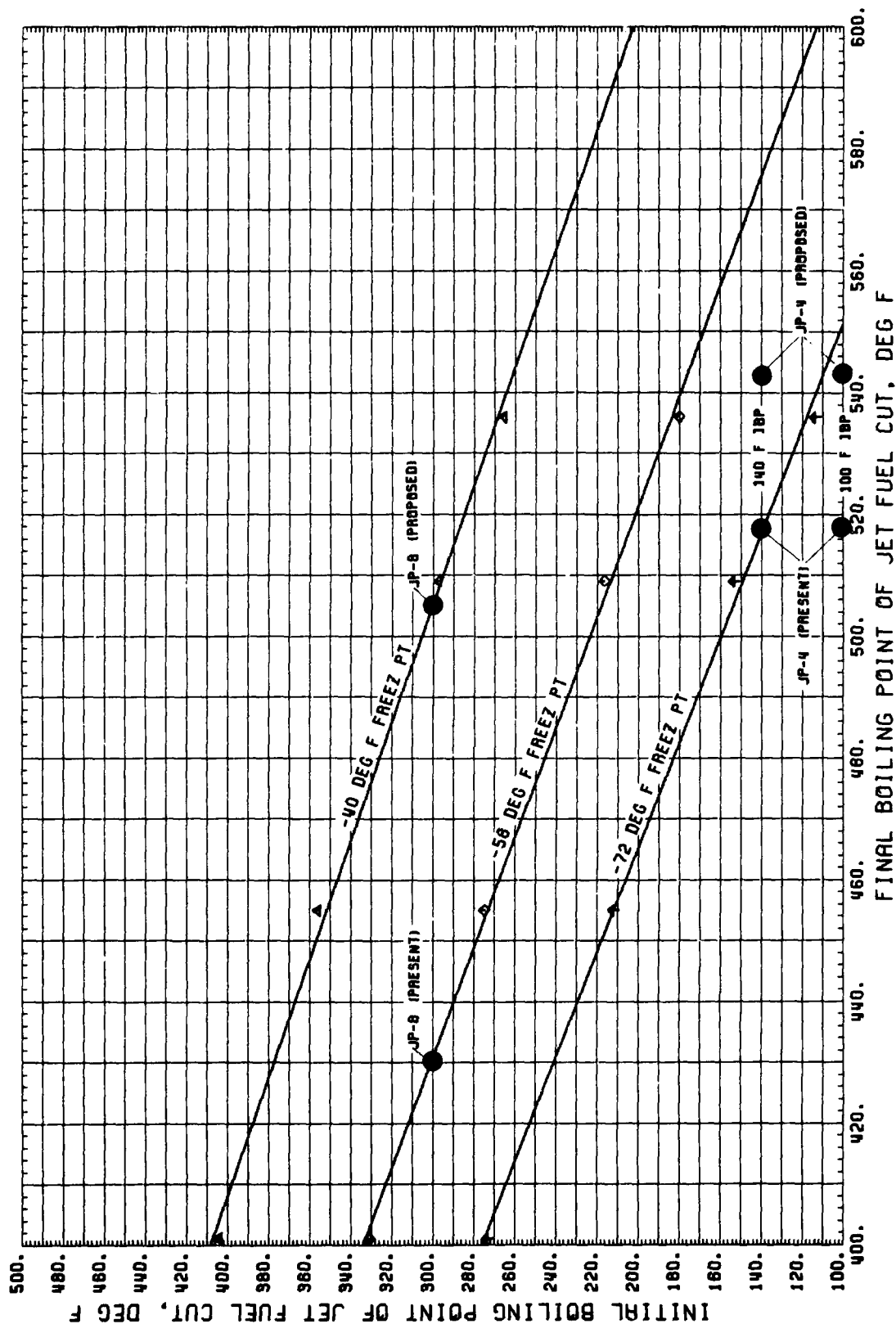


FIGURE 5.6

KUWAIT JET FUEL - FBP VS IBP AT CONSTANT FREEZING POINT



JP-8 under present specifications is limited by the -58°F freezing point to a FBP of 430°F . JP-8 under proposed specifications is limited to 505°F FBP by the -40°F freezing point specification. Maximum FBP's allowable under present and proposed specifications would be 572°F and 597°F , respectively.

Figure 5.7 displays the smoke point curves for Kuwait jet fuels. Since the smoke points are above 20 mm for any cut below 300°F IBP, smoke point is not a limit for Kuwait turbine fuels. For crudes where smoke point is limiting, the same procedure was used as described above for freezing point.

A portion of the Kuwait crude true boiling point (TBP) curve is given in Figure 5.8. Shown superimposed on the TBP curve are the cut points which satisfy the freezing point and IBP cuts for JP-4 and JP-8 as used in this study. These are the cut points indicated on Figure 5.6. The yields from a barrel of crude for each cut are then read off the TBP curve. Take the present specification JP-4 for example: A 100°F cut is 4.9% of the crude and a 518°F cut is 38.7% of the crude, hence a 100- 518°F JP-4 cut is the difference between those yields, or 33.8 vol% of the crude.

Table 5.2 displays the various volume percent yields for JP-4 and JP-8 obtained from Figure 5.8. Also shown are the yield increases for changing from present specifications to proposed specifications.

Increases from present yields to present and proposed specifications are also given for JP-4. However, these results are probably not meaningful because often the physical equipment and/or market requirements will not allow a given refiner to produce right up to the theoretical maximum yield of jet fuel from a given crude, or blend of crudes. Therefore, only the comparison of proposed specs. to present specs. are considered meaningful.

It should be mentioned here that seldom does a refinery run a single crude through a crude distillation unit to obtain the turbine fuel yields as described above. In most cases, several crudes are blended together due to equipment limitations. If one of the crudes is limiting due to smoke point or freezing point, a lighter FBP cut may be taken. The incremental volume available for turbine fuel from the remaining crudes would be lost to a heavier product.

In some cases, smoke point limiting turbine fuels are hydrotreated. This will change the theoretical yields of turbine fuel from crude. Also, a portion of the turbine fuel is the result of cracking heavier fuels, such as gas oils. However, the results of this study should be a good indication of the percentage increase in turbine fuel yield which would result from the proposed specification changes.

FIGURE 5.7
KUWAIT JET FUEL - SMOKE POINT VS INITIAL BOILING POINT

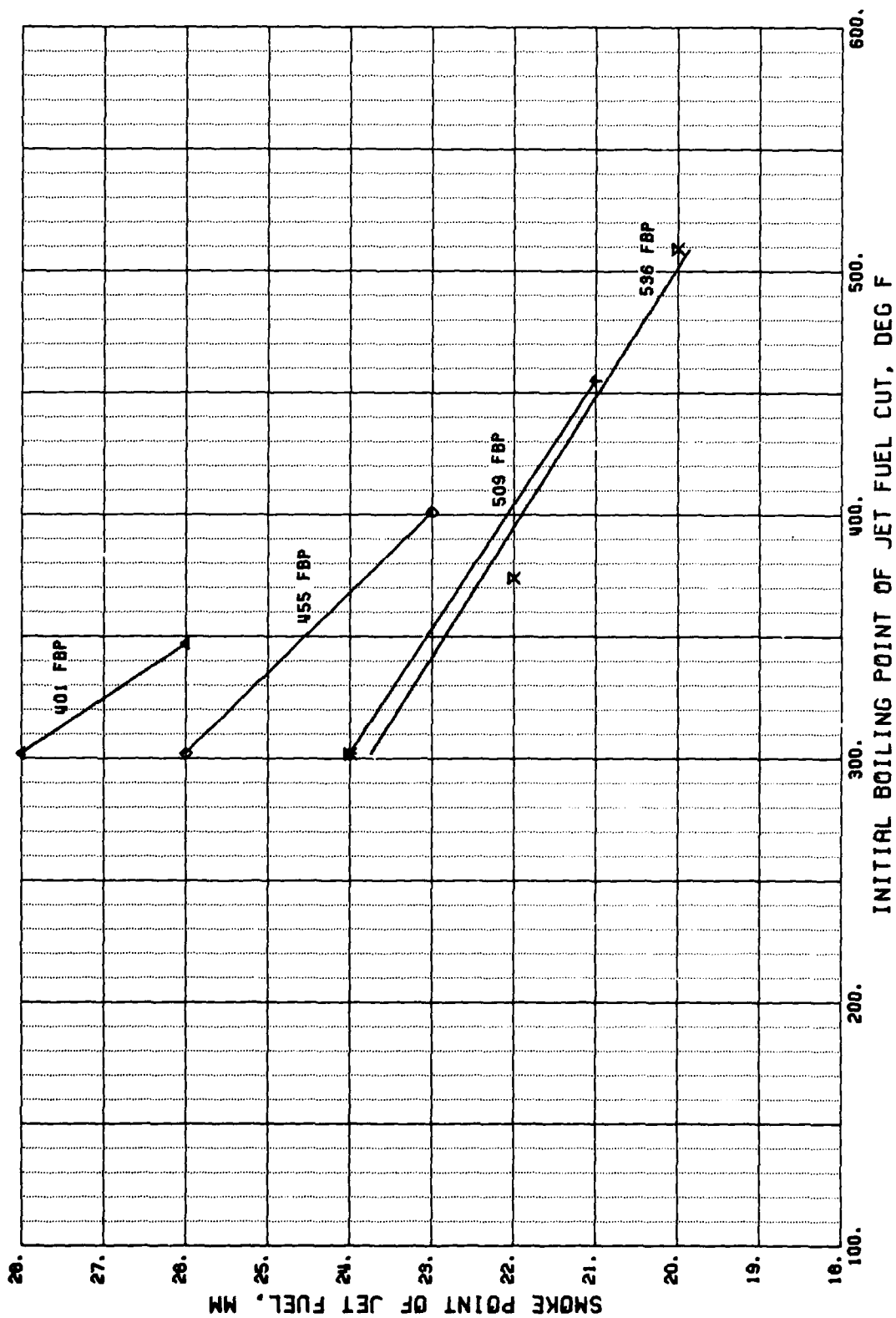


Table 5.2

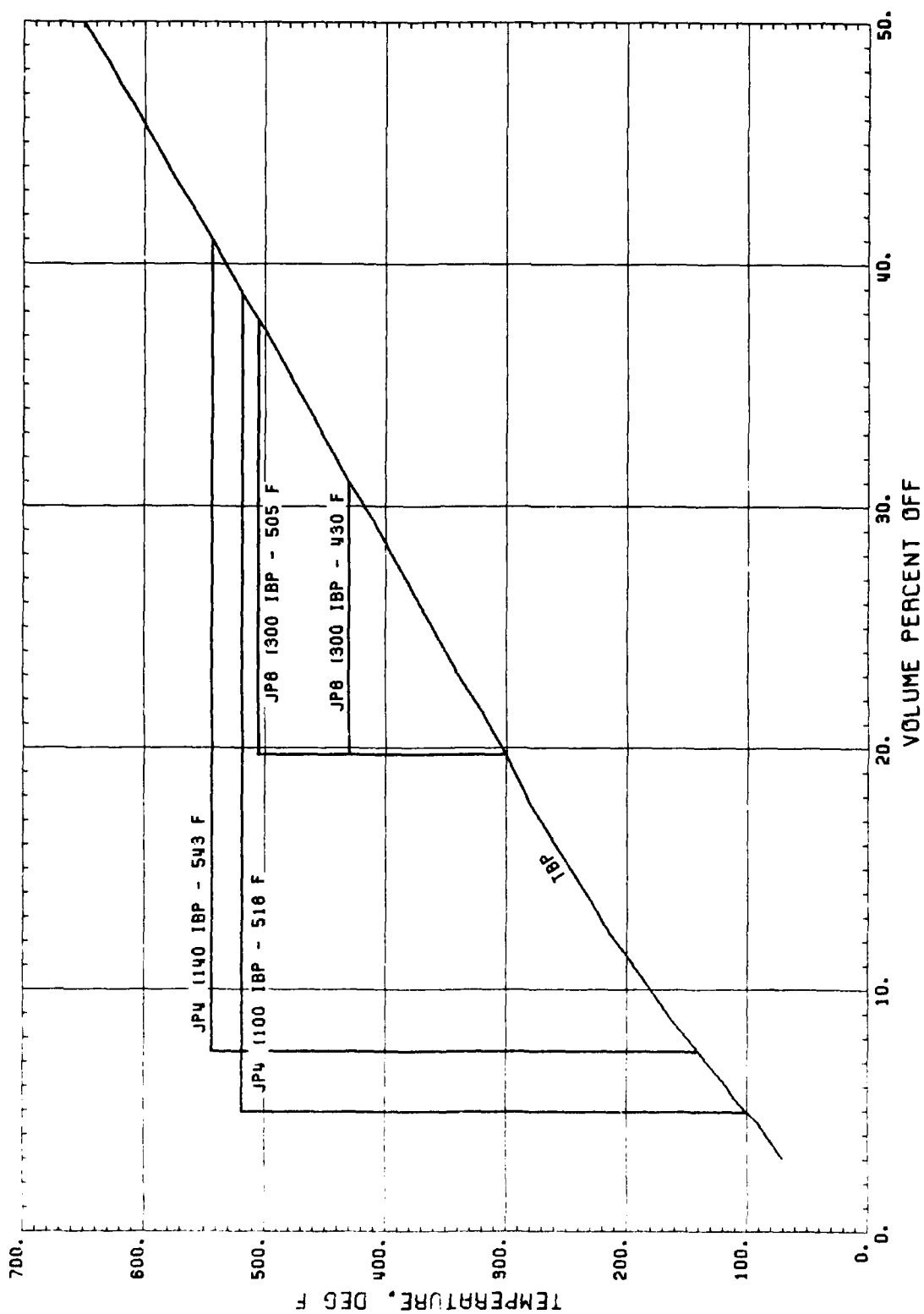
JP-4 YIELDS FOR KUWAIT CRUDE

<u>YIELDS, Vol % of Crude</u>	<u>100 °F IBP</u>	<u>140 °F IBP</u>
IBP-450°F Cut Point (Present Yields)	27.9	25.4
IBP-518°F Cut Point (Present Specs)	33.8	31.3
IBP-543°F Cut Point (Proposed Specs)	36.1	33.6
<u>INCREASE IN YIELDS, %</u>		
Proposed Specs vs Present Specs	6.8	7.3
Present Specs vs Present Yields	21.1	23.2
Proposed Specs vs Present Yields	29.4	32.3

JP-8 YIELDS FOR KUWAIT CRUDE

<u>YIELDS</u>	<u>VOL % CRUDE</u>
300-430°F Cut (Present Specs)	11.3
300-505°F Cut (Proposed Specs)	18.0
<u>INCREASE IN YIELD, %</u>	59.3

FIGURE 5.8
KUWAIT CRUDE TBP, 0 TO 50% RANGE



SECTION 6.

DEVELOPMENT OF HYDROGEN CONTENTS

Hydrogen content of turbine fuel cuts is not reported in crude assays. In order to determine the effect of jet fuel specification changes on hydrogen content, the hydrogen content of each cut had to be calculated.

ASTM D-3343 presents the following equation for hydrogen content of typical turbine fuels:

$$\begin{aligned} \text{WT\% H}_2 = & 0.06317 (\text{API}) - 0.041089 (\text{AROM}) \\ & + 0.000072135 (\text{AROM}) (\text{ABP}) + 0.00005684 (\text{API}) (\text{ABP}) \\ & - 0.0004960 (\text{API}) (\text{AROM}) + 10.56 \end{aligned}$$

Where: API = gravity, °API
 AROM = volume percent aromatics
 ABP = volume average boiling point

This equation was developed for typical wide-boiling turbine fuels, not for narrow cuts. It was used in this study because there was no other alternative. It is realized that the hydrogen contents calculated for these narrow cuts may be in error, but we were looking for changes in hydrogen, not absolute values.

Seven crudes were selected for hydrogen content determination:

- 1) Arabian Light
- 2) Iranian Light
- 3) Zueitina, Libya
- 4) Nigerian Light
- 5) West Texas Sour
- 6) West Delta Block, Louisiana
- 7) Wilmington East, California

Tables 6.1 - 6.7 contain the raw data and the calculated hydrogen contents of each cut for the seven crudes. (Although hydrogen contents are normally reported to two decimal places, these are shown to four decimal places. This does not signify accuracy but was used only for calculation purposes.) These calculated hydrogen contents were then plotted against cut FBP and/or IBP in a manner similar to that described in Section 5 to obtain the plots shown in Figures 6.1 - 6.7. From these curves, the predicted hydrogen contents could be obtained for each FBP developed in Section 5. (Actually, the equations of the curves were used to obtain the hydrogen contents to three decimal places.)

TABLE 6.1

CALCULATED WT % HYDROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

ARABIAN LT CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H₂, WT %</u>
302-347	52.0	325.	22.1	13.8455
302-401	50.2	352.	22.6	13.8180
302-455	48.4	379.	22.2	13.8219
302-509	46.3	406.	22.1	13.7849
302-536	45.3	420.	22.4	13.7580
347-401	48.7	374.	23.0	13.7915
374-536	43.1	456.	22.2	13.7432
401-455	45.1	428.	21.5	13.8056
401-509	43.2	456.	21.8	13.7629
455-509	41.7	481.	22.0	13.7386
509-536	38.6	523.	24.0	13.6056

TABLE 6.2

CALCULATED WT % HYDROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

IRANIAN LT CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H₂, WT %</u>
302-347	51.1	324.	17.9	13.9582
302-401	49.0	353.	20.1	13.8359
302-455	46.9	381.	20.7	13.7752
302-509	44.9	408.	22.6	13.6708
302-536	44.0	420.	23.1	13.6364
347-401	47.3	375.	22.0	13.7312
374-536	41.5	455.	25.0	13.5335
401-455	43.4	429.	21.7	13.6726
455-509	39.6	483.	27.6	13.4342
509-536	36.7	522.	27.4	13.3744

TABLE 6.3

CALCULATED WT % HYROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

ZUEITINA (LIBYAN) CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H2, WT %</u>
320-356	51.1	355.	13.2	14.2802
356-428	46.8	405.	12.4	14.1586
428-482	43.0	474.	12.6	14.0792
320-482	46.7	415.	12.6	14.1792

TABLE 6.4

CALCULATED WT % HYROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

NIGERIAN LT CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H2, WT %</u>
300-500	39.8	410.	19.0	13.4078
375-480	37.7	435.	21.2	13.2714
375-530	36.1	464.	25.2	13.1493

TABLE 6.5

CALCULATED WT % HYROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

WEST TEXAS SOUR CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H2, WT %</u>
302-401	46.0	354.	19.5	13.6432
302-455	43.9	381.	21.2	13.5338
302-509	42.0	404.	24.4	13.3778
374-482	40.0	427.	25.3	13.2954
374-536	38.1	482.	30.1	13.2516

TABLE 6.6
CALCULATED WT % HYROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

LOUISIANA CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H2, WT %</u>
175-300	57.1	250.	4.8	14.7318
250-375	47.8	320.	9.5	14.0527
300-500	38.5	430.	18.0	13.4080
375-480	37.3	436.	17.9	13.3369
375-530	35.8	467.	23.2	13.1881

TABLE 6.7
CALCULATED WT % HYROGEN CONTENT OF JET FUELS
USING API CORRELATION D-3343

WILMINGTON EAST CRUDE

<u>CUT POINT DEG F</u>	<u>GRAVITY DEG API</u>	<u>AVE BP DEG F</u>	<u>AROMATICS VOL %</u>	<u>CALCULATED H2, WT %</u>
175-300	54.6	242.	3.9	14.5623
250-375	46.4	323.	8.7	13.9880
300-500	37.7	421.	20.9	13.2288
375-480	35.9	434.	22.7	13.0871
375-530	34.1	462.	29.1	12.8915

FIGURE 6.1

ARABIAN LT JET FUEL

(WT % H₂ CALCULATED FROM API GRAV. AVE BP. & % AROM)

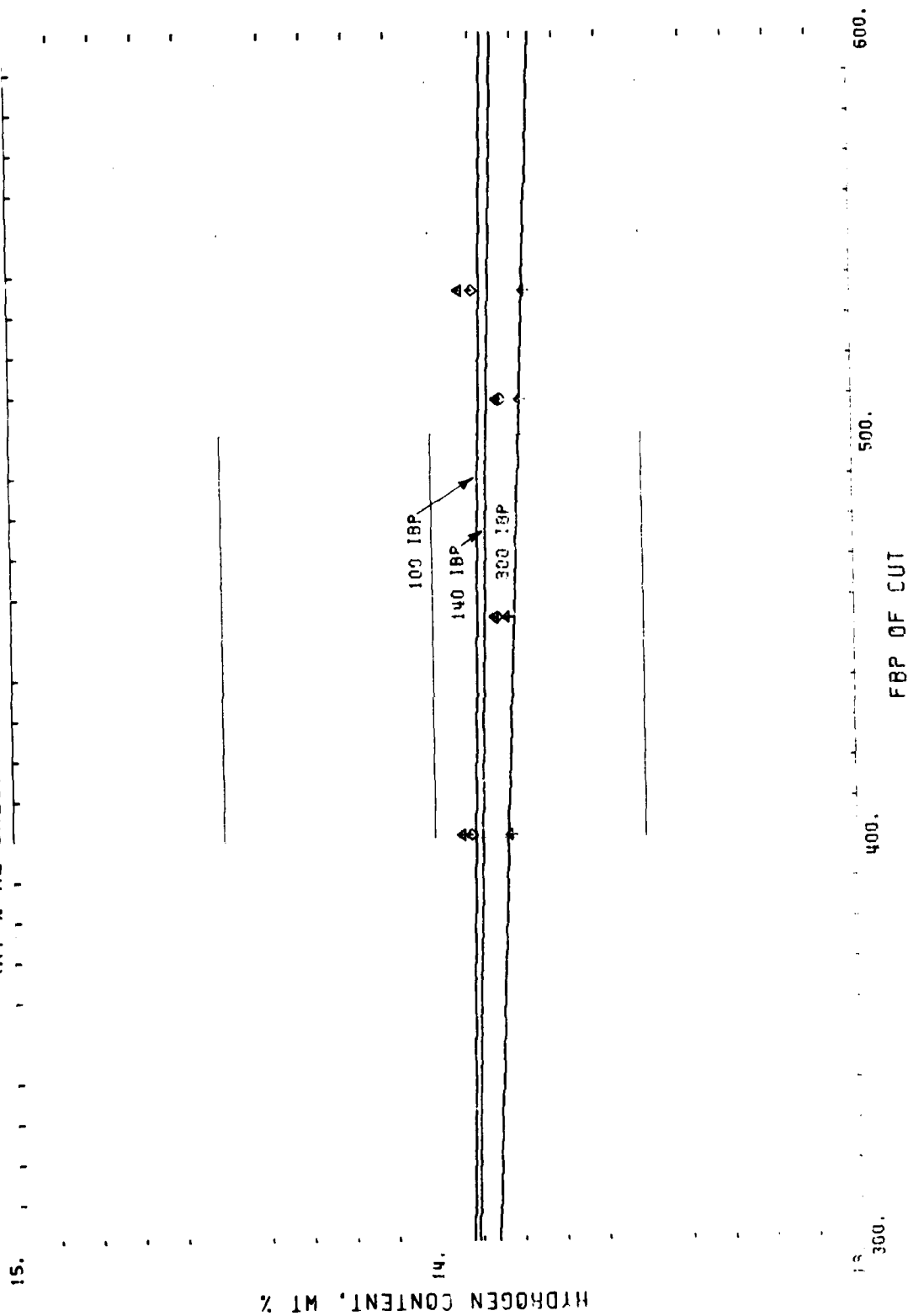


FIGURE 6.2
 IRANIAN LT JET FUEL
 (WT % H₂ CALCULATED FROM API GRAV, AVE BP, & % AROM)

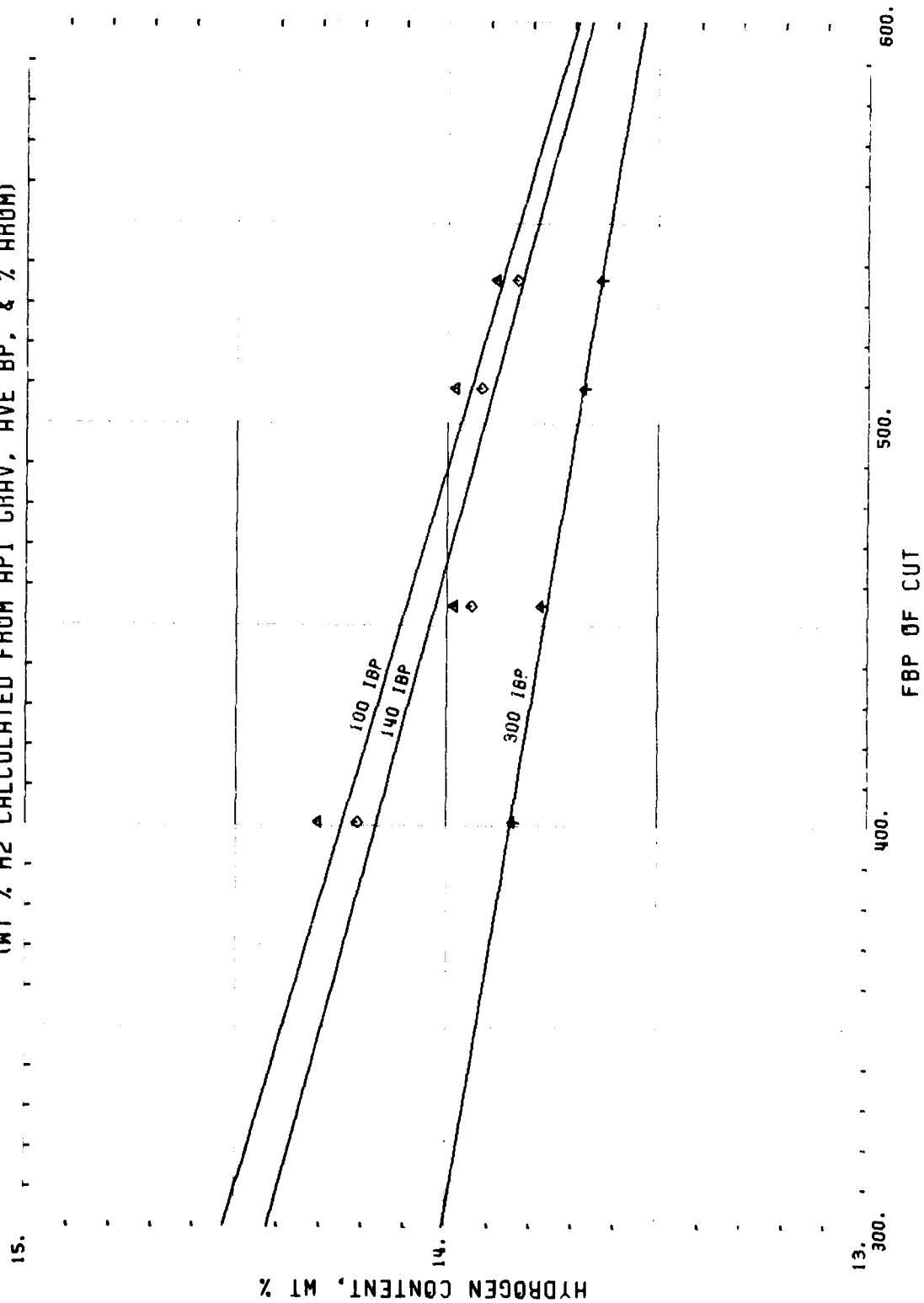


FIGURE 6.3
ZUEITINA LT JET FUEL
(WT % H₂ CALCULATED FROM API GRAV, AVE BP, & % AROM)

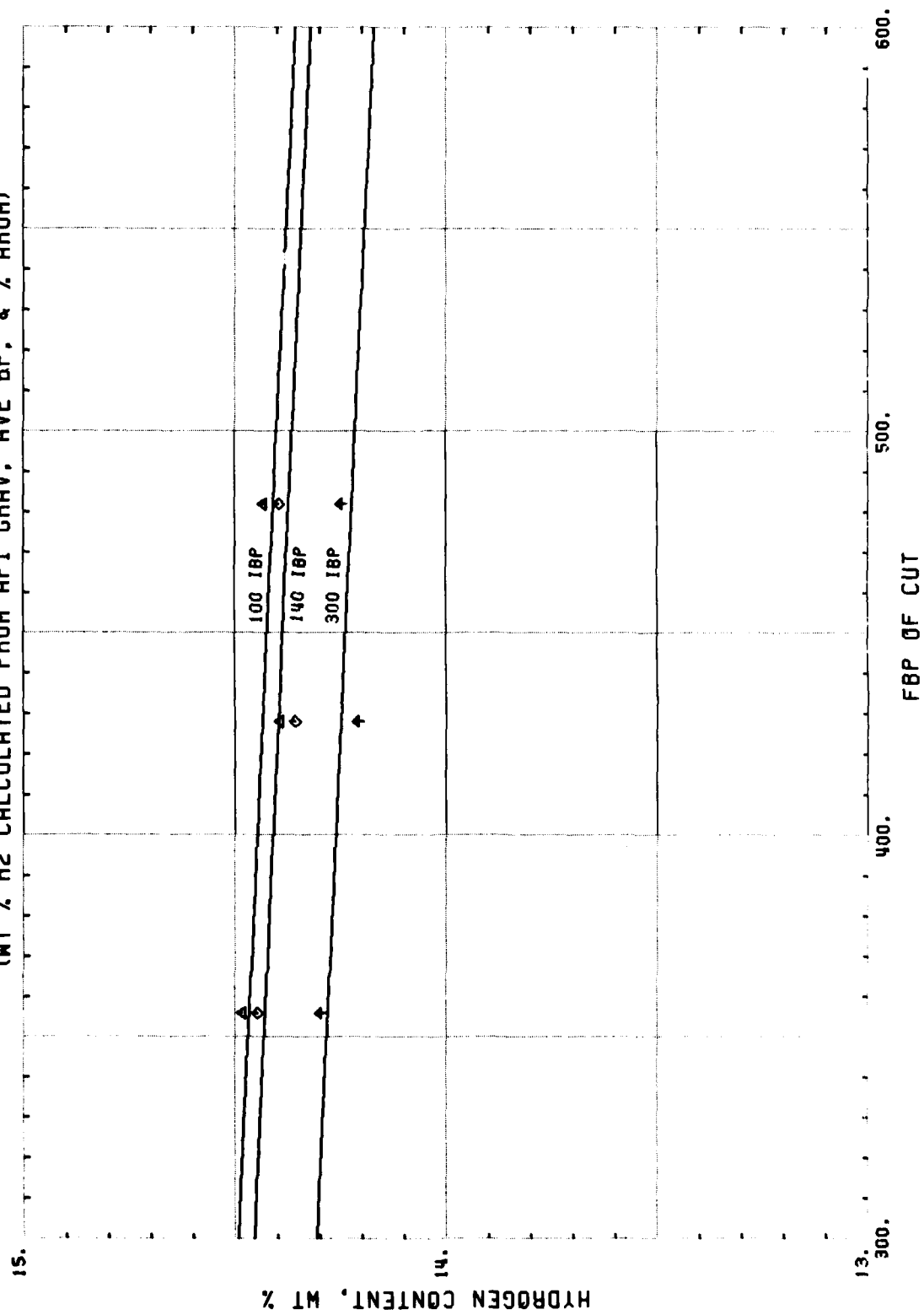


FIGURE 6.4
NIGERIAN LT JET FUEL
(WT % H₂ CALCULATED FROM API GRAV, AVE BP, & % AROM)

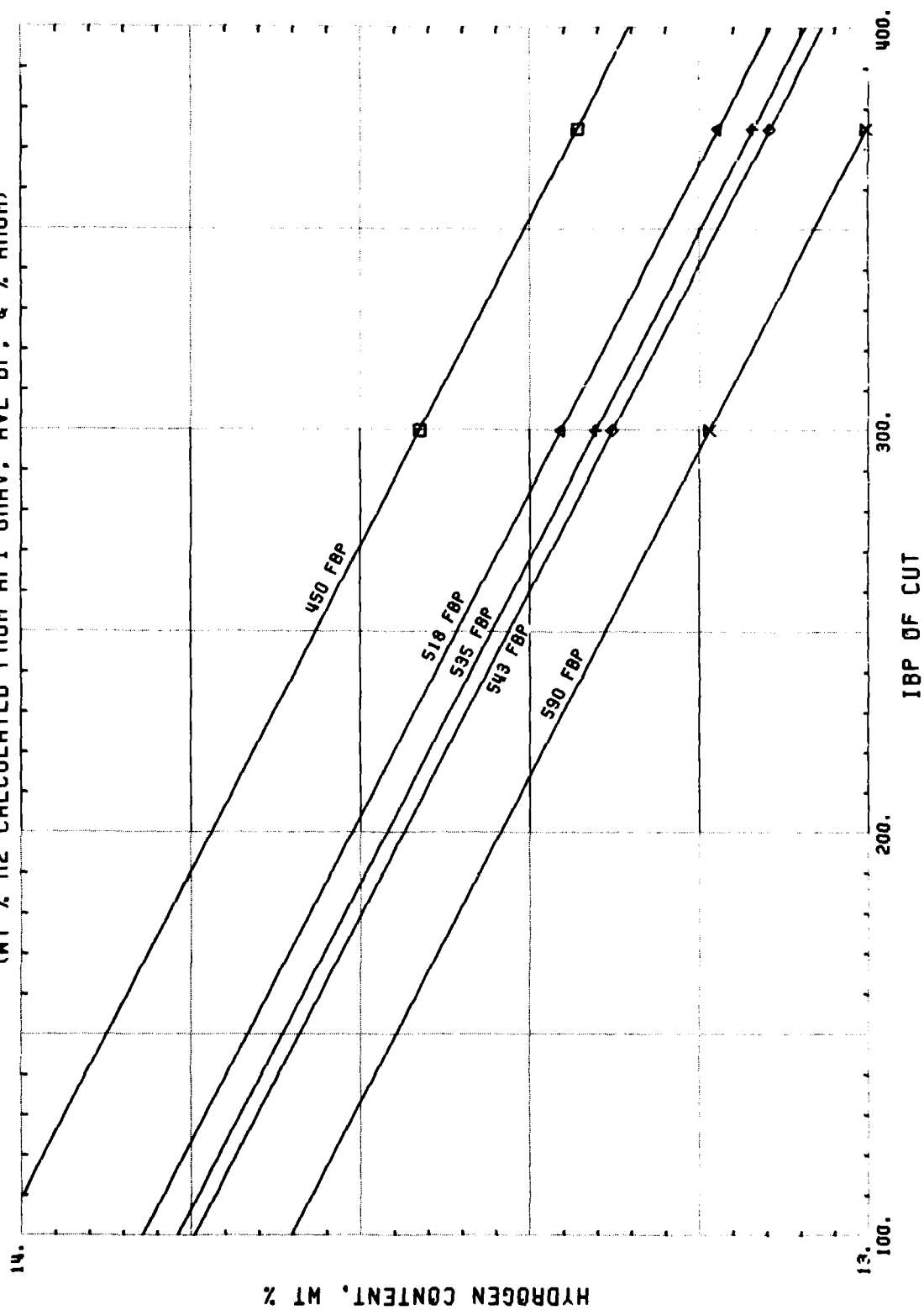


FIGURE 6.5
WEST TEXAS SOUR JET FUEL
(WT % H₂ CALCULATED FROM API GRAV, AVE BP, & % AROM)

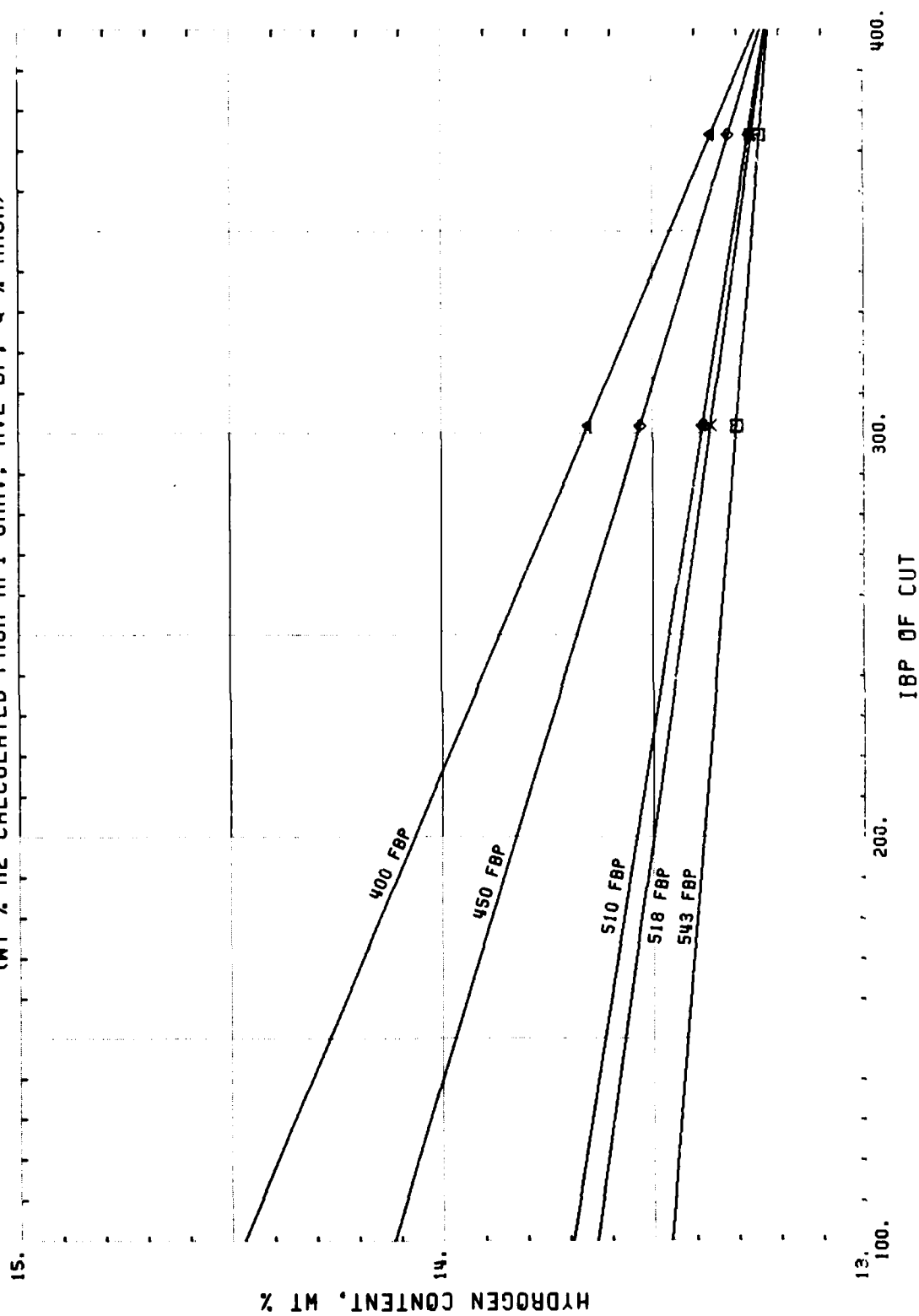


FIGURE 6.6
LOUISIANA JET FUEL

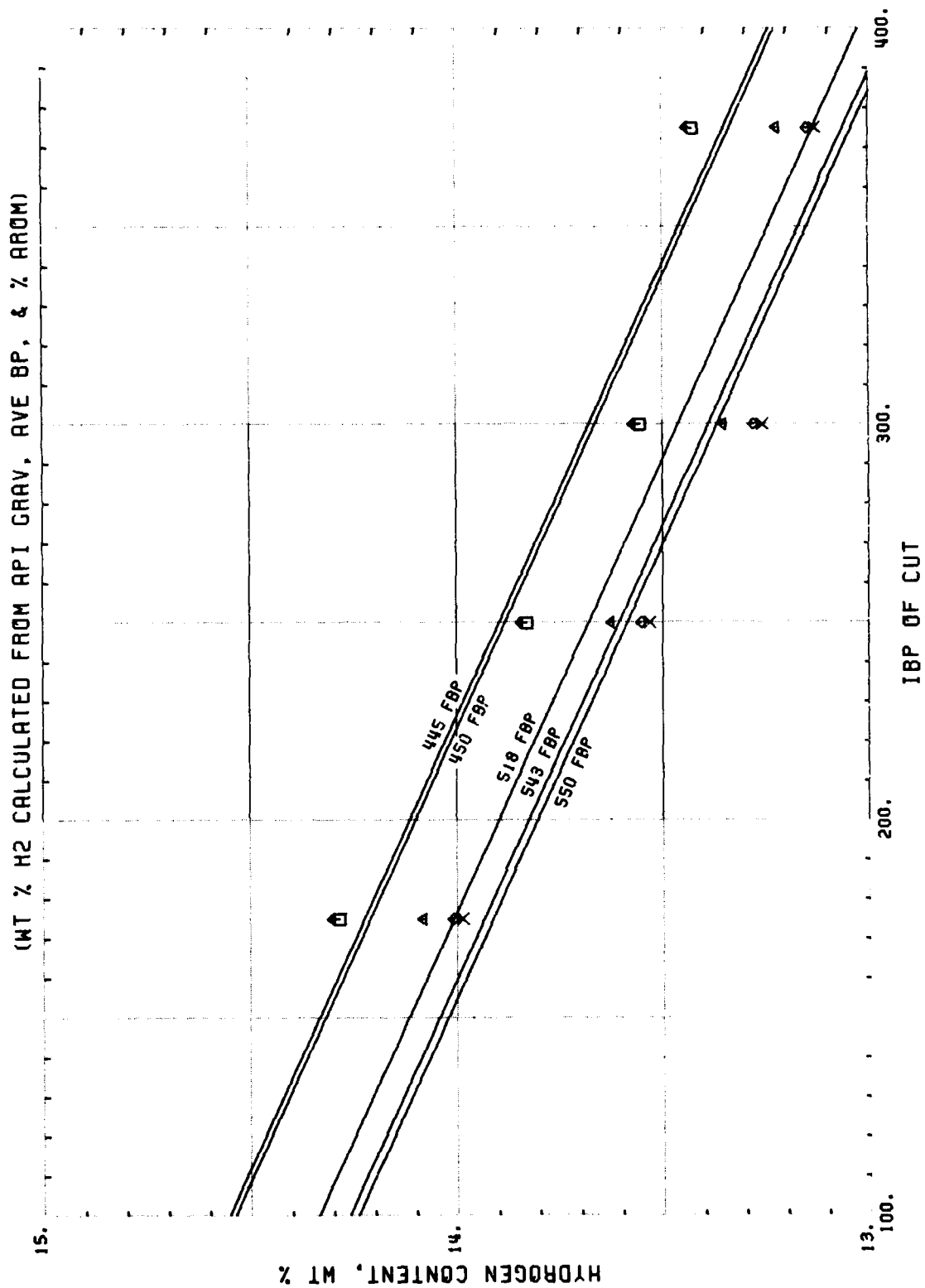
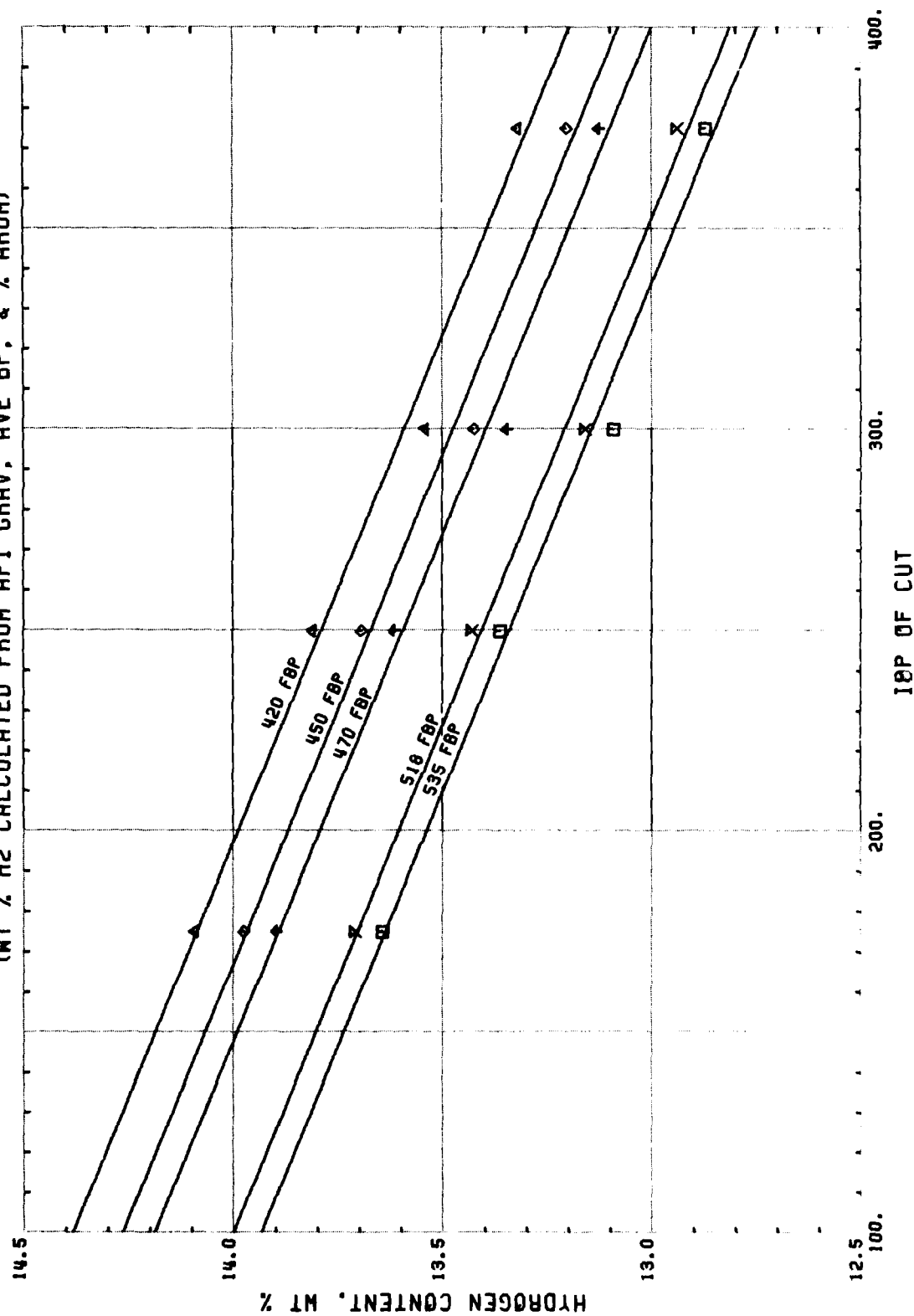


FIGURE 6.7
WILMINGTON JET FUEL
(WT % H₂ CALCULATED FROM API GRAV, AVE BP, & % AROM)



SECTION 7.

AVERAGING CALCULATED RESULTS

A. Yield Change Data

The Department of Energy publishes a monthly petroleum statement entitled "Energy Data Reports". The July, 1978 report (published November, 1978) was used as a source basis for averaging the calculated results of this study. The July, 1978 report contains the production of crude petroleum in the U. S. by state and PAD district on page 7 and the imports of foreign crude oil by country of origin on page 17. The cumulative data for January-July (inclusive), 1978 was used.

Results for crudes from a single country were arithmetically averaged before including in the final averaging.

Table 7.1 contains all of the results by individual domestic crudes and by foreign countries. The first column contains the actual January through July, 1978 crude volume in 1000 barrels. For domestic crudes, this represents 62.7% of the total U. S. production during that period. For foreign crude, it represents 90.1% of the total imported. The gross assumption was made that the crude shown would represent the total area of its origin.

In column 2, the total barrels represented are given as volume percent prorated so that total domestic and total foreign each add up to 100%.

Domestic and foreign data results are then averaged on the basis of their respective volume percent of total domestic or foreign crude usage.

During this seven month period, domestic crude accounted for 58.357% of total U. S. crude usage. At the bottom of Table 7.1, domestic and foreign results are averaged on the basis of total domestic and foreign crude used.

Six columns of data are given for JP-4 as indicated by the headings of Table 7.1 and as discussed previously. The first two columns are probably the most meaningful as they compare the theoretical change from present to proposed specifications.

The volume average yield increase for JP-8 is 61.65%. However, data for Louisiana crude and West Texas crude appear to be out of line with the other data. If we throw these two data points out, the average becomes 40.72%.

A list of the product specification which was limiting on each crude for each turbine fuel considered is given in Table 7.2. JP-4 is not broken down to 100°F and 140°F IBP since this had no effect on specification limitations in this study.

Note that FBP of the cut was the predominant limiting specification for JP-4 (84% of all crudes examined). Freezing point was the limiting factor in JP-8 fuel for about 90% of the imported crudes while smoke point was limiting for JP-8 in 66% of the domestic crudes evaluated.

B. Hydrogen Content Data

Table 7.3 contains the hydrogen content results for the seven crudes selected for this portion of the study as discussed in Section 6. Column one gives the total barrels of crude and Column two contains the volume percent prorated to 100% for these seven crudes. The bottom row contains the averages of the data for the seven crudes.

Table 7.1

Jet Fuel yield increases
(Volume Percent)

Fuel Type Change to: Change From: JP-4 1BP, °F	Jan-July, 1978 1000 Bbl Vol %	JP-4				JP-8			
		Proposed Specifications		Present Specifications		Proposed Specifications		Present Specifications	
		100	140	100	140	100	140	100	140
Domestic Crudes									
Alaska	243,829	8.5	8.7	28.5	30.5	59.1	41.6	64.8	
California									
Southern	55,214	8.2	8.5	39.2	41.6	50.6	53.7	60.0	
Central Coastal	36,529	10.2	10.4	29.2	30.2	42.5	43.8	52.4	
East Central	108,007	0.0	0.0	9.9	5.4	9.9	5.4	53.4	
Louisiana	313,548	15.2	15.3	58.1	58.8	82.0	83.0	112.0	
Texas									
District 06	34,730	7.3	7.8	23.5	25.7	32.5	35.5	38.1	
East Texas	36,569	5.9	6.5	17.5	18.9	24.5	26.5	36.7	
West Texas	186,458	6.5	6.7	20.6	22.5	28.1	30.5	105.7	
Rocky Mountain	136,585	9.5	9.9	32.2	33.7	44.9	46.8	16.4	
Total Represented	1,151,069	9.17	9.44	34.01	34.87	46.94	48.28	81.76	
Other Domestic	685,018								
Total Domestic	1,836,087	58.557 (of total used)							
Imported Crudes									
Algeria	139,865	4.5	4.9	16.2	17.8	21.5	25.6	14.7	
Canada	50,736	6.0	6.4	19.1	20.7	26.2	28.5	7.1	
Capeon	7,829	6.5	6.7	25.5	26.4	33.5	34.9	19.6	
Indonesia	104,352	11.7	12.8	24.8	21.1	58.6	55.5	62.4	
Iran	119,156	10.53	7.2	22.5	25.2	30.6	34.2	56.0	
Iraq	11,740	1.057	6.1	20.8	22.4	26.2	30.5	35.0	
Kuwait	1,718	0.152	7.5	21.1	23.2	29.4	32.5	59.5	
Libya	119,683	10.570	6.2	20.8	22.3	28.4	30.4	36.1	
Malaysia	7,146	0.651	5.1	30.1	30.8	40.4	41.5	29.6	
Nigeria	171,057	15.107	8.8	27.1	28.5	38.5	40.2	29.5	
North Sea	56,177	4.961	6.0	19.0	20.4	26.1	28.0	37.6	
Oman	13,505	1.175	8.5	22.0	23.4	31.9	33.8	33.7	
Saudi Arabia	218,141	19.265	6.9	21.2	23.3	29.6	32.5	27.2	
United Arab Emirates	78,436	6.937	6.5	21.2	23.0	28.6	31.0	35.6	
Venezuela	52,985	2.915	37.6	11.2	0.0	35.7	37.6	95.5	
Total Represented	1,152,526	7.553	8.48	21.50	22.40	30.61	32.48	55.17	
Other Imported	125,792								
Total Imported	1,256,118	41.643 (of total used)							
Total Crude Used	3,016,588	100.000	8.44	28.83	29.68	40.14	11.70	61.65	

TABLE 7.2
LIMITING SPECIFICATION TO TURBINE FUEL CUT
(CRUDES LISTED BY FIELD NAME)

	JP-4		JP-8	
	PRESENT	PROPOSED	PRESENT	PROPOSED
Domestic Crudes				
Prudhoe Bay, AK	FBP	FBP	SMK PT	SMK PT
Wilmington East, CA	SMK PT	SMK PT	SMK PT	SMK PT
Ventura, CA	FBP	FBP	SMK PT	SMK PT
Midway Sunset, CA	SMK PT	SMK PT	SMK PT	SMK PT
West Delta Block, LA	FBP	FBP	SMK PT	SMK PT
Hawkins Mix, TX	FBP	FBP	FRZ PT	FRZ PT
East Texas, TX	FBP	FBP	FRZ PT	FBP
West Texas Sour, TX	FBP	FBP	SMK PT	SMK PT
Rocky Mtn Sour, WY	FBP	FBP	FRZ PT	FRZ PT
Domestic Crudes Limited by:				
Final Boiling Point	7	7	0	1
Freezing Point	0	0	3	2
Smoke Point	2	2	6	6
Total	9	9	9	9
Imported Crudes				
Arabian Lt	FBP	FBP	FRZ PT	FRZ PT
Iranian Lt	FBP	FBP	FRZ PT	FRZ PT
Khafji	FRZ PT	FRZ PT	FRZ PT	FRZ PT
Kuwait	FBP	FBP	FRZ PT	FRZ PT
Murban	FBP	FBP	FRZ PT	FRZ PT
Kirkuk	FBP	FBP	FRZ PT	FRZ PT
Brega	FBP	FBP	FRZ PT	FRZ PT
Dubai	FBP	FBP	FRZ PT	FBP
Basrah	FBP	FBP	FRZ PT	FRZ PT
Ardeshir	FBP	FBP	FRZ PT	FRZ PT
Oman	FBP	FBP	FRZ PT	FRZ PT
Rostam	FBP	FBP	FRZ PT	FRZ PT
Brut Mandji	FBP	FBP	FRZ PT	FRZ PT
Hassi-Messaoud	FBP	FBP	FRZ PT	FRZ PT
Es Sider	FBP	FBP	FRZ PT	FRZ PT
Nigerian Lt	FBP	FBP	FRZ PT	FRZ PT
Tia Juana Med	FRZ PT	FBP	FRZ PT	FRZ PT
Pembina	FBP	FBP	FBP	FRZ PT
Tapis	FBP	FBP	FRZ PT	FRZ PT
Seria	FBP	SMK PT	SMK PT	SMK PT
Sumatra Lt	FRZ PT	FRZ PT	FRZ PT	FRZ PT
Piper	FBP	FBP	FRZ PT	FBP
Imported Crudes Limited by:				
Final Boiling Point	20	20	1	2
Freezing Point	3	2	21	20
Smoke Point	0	1	1	1
Total	23	23	23	23

Table 7.3

Change in Hydrogen Content of Jet FuelJP-4 Fuel

IBP of Fuel, °F	<u>100</u>	<u>140</u>
Proposed Specifications	13.972	13.868
Present Specifications	<u>14.022</u>	<u>13.913</u>
Change	-0.050	-0.045
	(-0.36%)	(-0.32%)
Proposed Specifications	13.972	13.868
Present Yields	<u>14.212</u>	<u>14.112</u>
Change	-0.240	-0.244
	(-1.7%)	(-1.7%)
Present Specifications	14.022	13.913
Present Yields	<u>14.212</u>	<u>14.112</u>
Change	-0.190	-0.199
	(-1.3%)	(-1.4%)

JP-8 Fuel

Proposed Specifications	13.532
Present Specifications	<u>13.703</u>
Change	-0.171
	(-1.2%)

Table 7.4 shows the effect the changes in specifications would have on hydrogen contents of JP-4 and JP-8. Since these hydrogen contents were calculated from physical property data rather than being measured, the absolute values are not reliable. However, the percent changes due to specification changes are probably reasonable.

Hydrogen content changes range from -0.32% to -1.7% for JP-4 and is -1.2% for JP-8. These are percent changes in hydrogen content not differences, so one can see that the proposed change in specifications would have almost no effect on hydrogen content of the turbine fuels.

Table 7.4

USAF Fuel/Engine/Airframe Trade-Off Study

Calculated Hydrogen Contents of Jet Fuel from Selected Crudes, Wt %

JP-4 IBP	Crude	1000 Bbl ^b	Vol % ^c	JP-4			JP-8		
				Present		Proposed Specifications	Present		Proposed Specifications
				100	140		100	140	
	Arabian Light	218,141	18.436	13.896	13.879	13.885	13.865	13.880	13.860
	Iranian Light	119,156	10.070	14.110	14.042	13.921	13.869	13.851	13.806
	Zuetina	119,683	10.115	14.425	14.388	14.394	14.357	14.383	14.346
	Nigerian Light	171,057	14.456	14.024	13.925	13.858	13.759	13.797	13.698
	West Texas Sour	186,458	15.758	14.118	14.002	13.637	13.459	13.582	13.428
	Louisiana Delta	313,548	26.499	14.538	14.364	14.336	14.161	14.261	14.087
	Wilmington, California	55,214	4.666	14.265	14.107	13.999	13.842	13.933	13.775
	Total	1,183,257	100.000						
	Volume Average Hydrogen Content, wt %			14.212	14.112	14.022	13.913	13.972	13.868
							13.703		13.532

^a 450°F, FBP.^b January-July 1978 consumption in USA of crudes by country or state. Monthly Energy Review, January 1979, DOE.^c Prorated to 100%.

SECTION 8.

DEVELOPMENT OF COSTS

Some years ago, when crude supply and refining capacity exceeded demand, it was often said that the "incremental barrel" cost very little more to produce than the normal product demand. This "incremental barrel" product would be sold to cut-rate outlets which often produced "gas-war" pricing.

The idea might occur that increasing end-point on aviation turbine fuels slightly and thereby increasing volume incrementally would have the same pricing effect as the "incremental barrel" of the Fifties. In today's environment, this is not true because demand exceeds supply. As a nation, we are crude-short and refining capacity is strained to the limit.

Two factors restricting refining capacity are:

- 1) Environmental pressure combined with strict EPA regulations make it very difficult and costly to build a new refinery today.
- 2) New refineries and even new units in existing refineries are harder to justify due to current projections indicating a downturn in gasoline demand over the next decade. Also, it may not be possible to locate assured crude supplies for a new refinery today.

Increased turbine fuel supply must be purchased, therefore, at the expense of decreased gasoline, diesel, or heating oil supply.

The Monthly Energy Review published monthly by the Department of Energy contains retail and wholesale prices on aviation turbine fuels, heating oil, and diesel. Figures 8.1-8.4 display the wholesale prices of naphtha-type jet fuel, kerosene type jet fuel, heating oil, and diesel, respectively for July 1975 through November 1978.

Prices for the four years (1975-1978) are plotted along with the least-mean-squares linear regression curve. Variations in price appear to be cyclical with respect to seasons, so prices for December 31, 1978 were calculated for each fuel using the equations of the LMS lines. These prices are given below:

<u>Fuel</u>	<u>Wholesale Price, ¢/gal</u>
JP-4	38.92
JP-8	40.86
Heating Oil	38.87
Diesel	42.50

The low and high turbine fuel yield increases reported earlier in Section 7 are as follows:

	<u>Yield Increase, Vol %</u>	
	<u>Low</u>	<u>High</u>
JP-4	8.49	9.04
JP-8	40.72	61.65

FIGURE 8.1
NAPHTHA TYPE JET FUEL
WHOLESALE PRICES OVER LAST FOUR YEARS

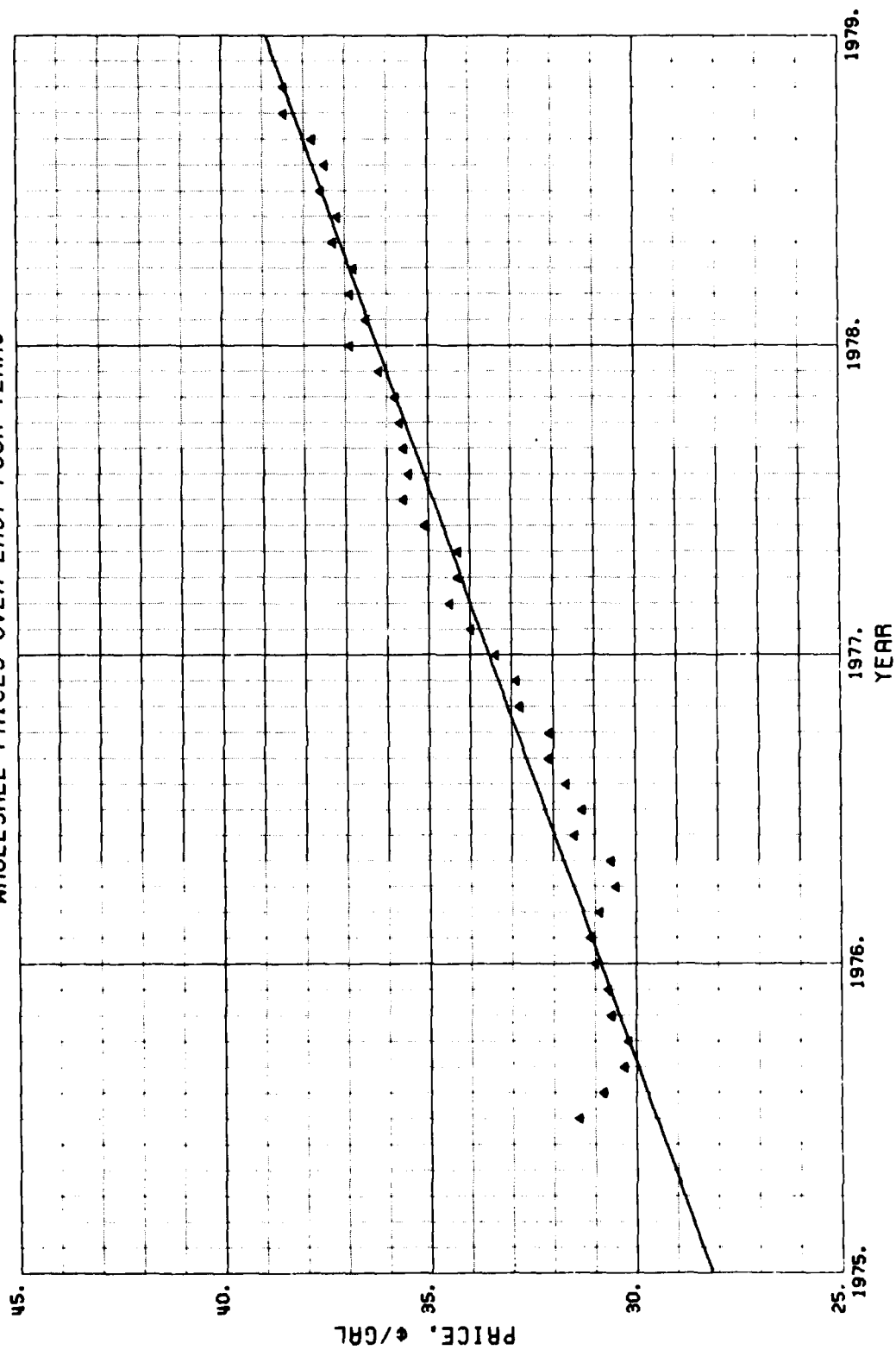


FIGURE 8.2
KEROSENE TYPE JET FUEL

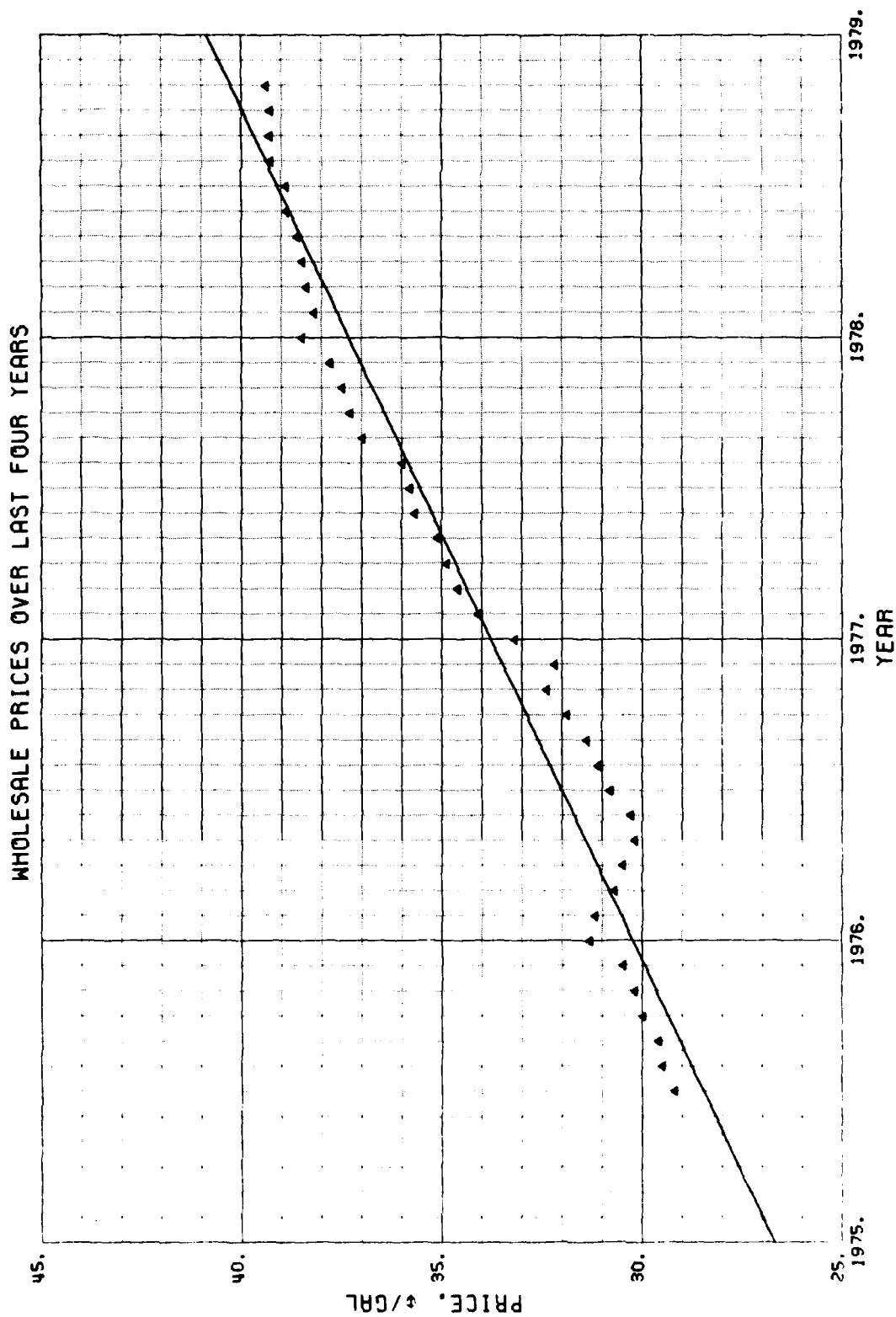


FIGURE 8.3
HEATING OIL

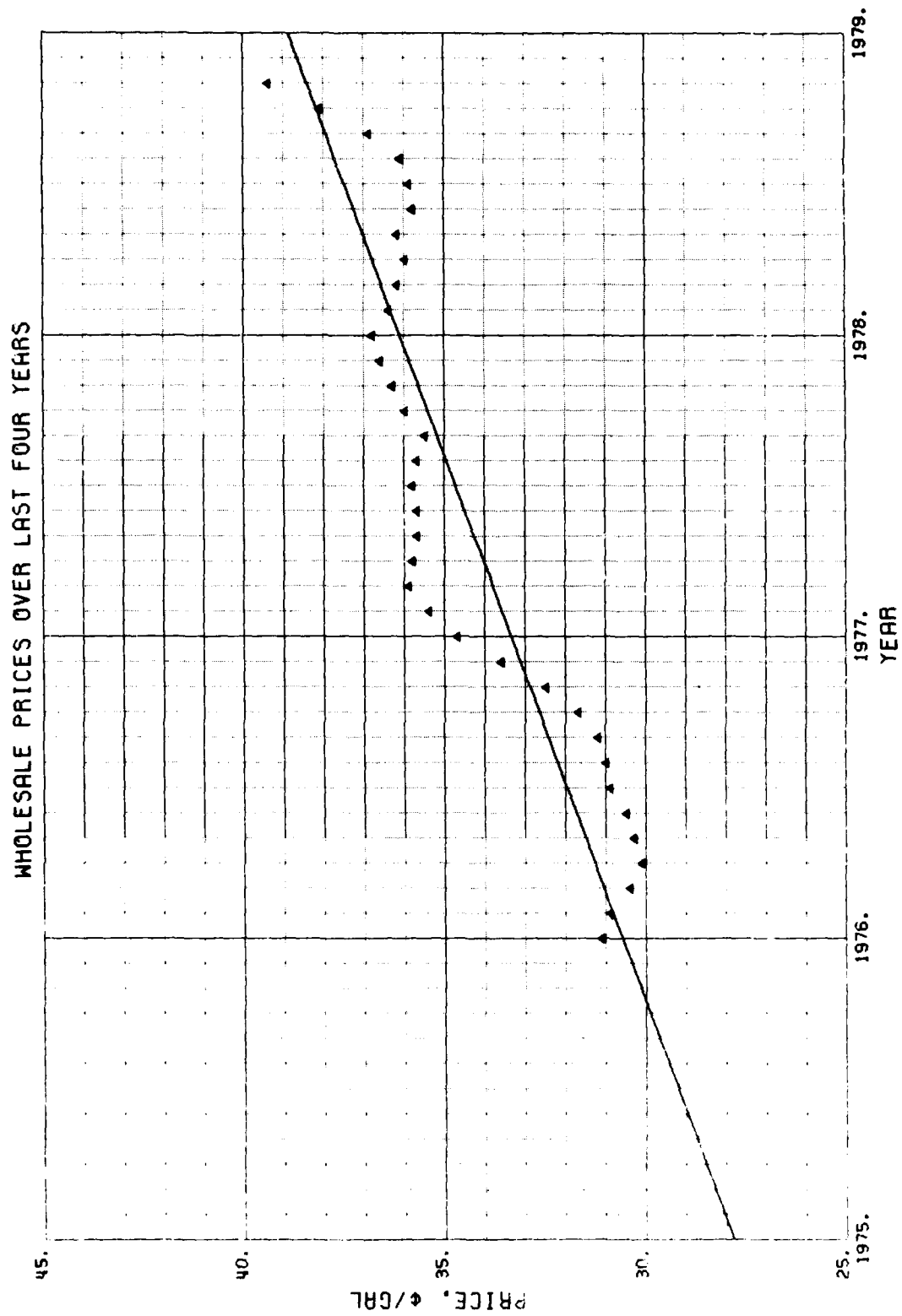
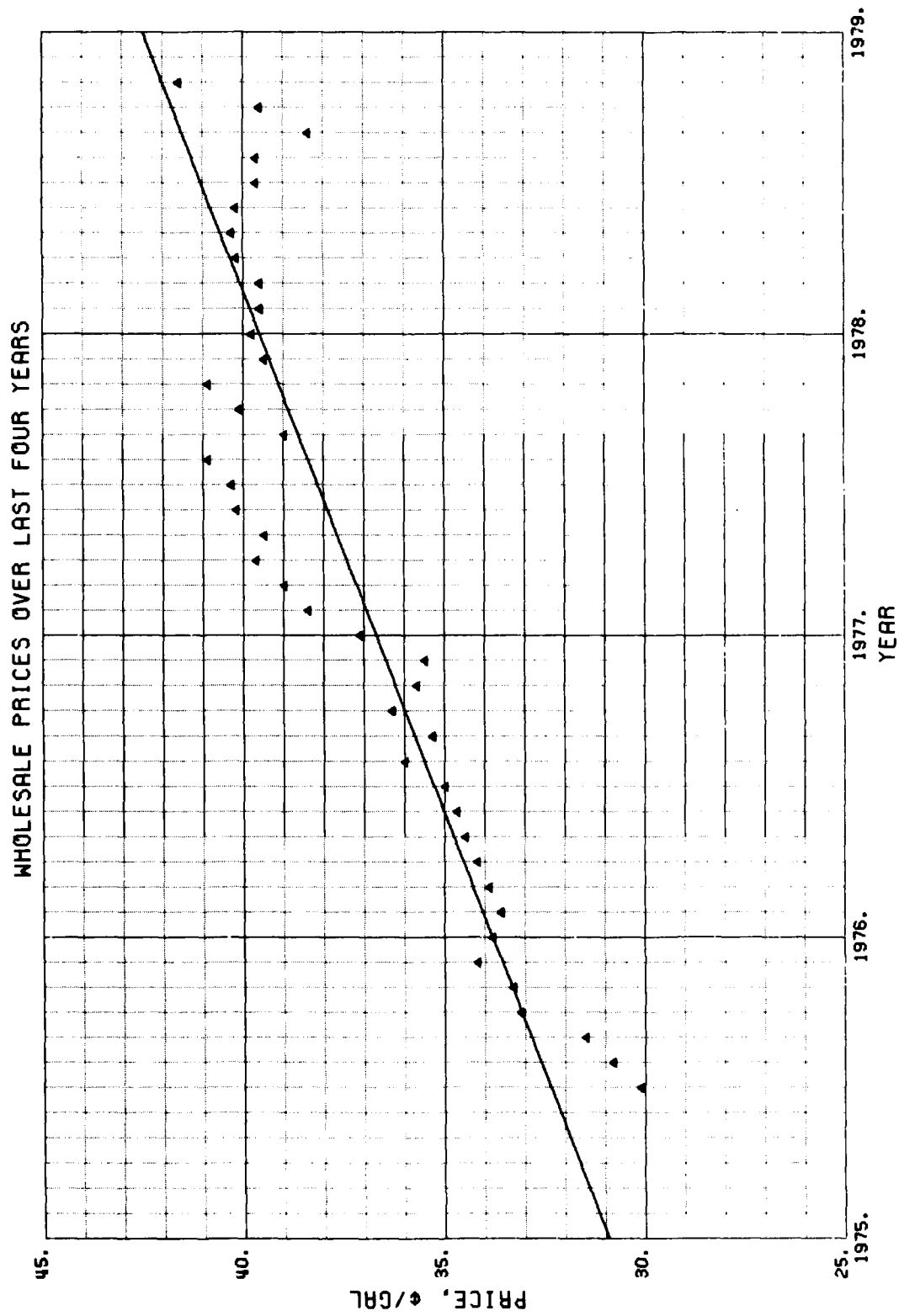


FIGURE 8.4
DIESEL FUEL



If we assume that the volume increase in turbine fuel will be composed of either diesel fuel or heating oil, and utilizing the wholesale prices listed above, the following price changes have been calculated:

	Price Change, ¢/gal	
	Low	High
<u>Using Diesel Prices</u>		
JP-4	0.30	0.32
JP-8	0.67	1.01
<u>Using Heating Oil Prices</u>		
JP-4	0.0	0.0
JP-8	-0.81	-1.23

On a percentage basis, these price changes amount to a maximum fluctuation of zero to +0.8 percent for JP-4 and -3.0 to +2.5 percent for JP-8.

Thus, it appears that a significant increase in turbine fuels could be realized if the specifications for JP-4 and JP-8 were relaxed as proposed for essentially no change in turbine fuel price. However, fuel prices are not constant. The new and growing demand for automotive diesel fuel for the private sector plus the current shortfall of diesel fuel for the trucking industry could cause middle distillate fuel prices to rise faster than other petroleum products.

The development of this study occurred prior to the publication by DOE of fuel prices for 1979 and prior to much of the middle east conflicts. The product prices analyzed above were taken during a time of relative price stability. The fuel prices published by DOE for 1979 show such a rapid escalation that they cannot be used to predict relative prices between products. Figures 8.5-8.8 display the prices the same as Figures 8.1-8.4 except that they are extended to November 1979.

It was suggested that if crude price fluctuation were removed from product prices, the product prices might show less scatter. Figure 8.9-8.12 display the differential price between the four products and average crude prices published by DOE. However, this exercise only increased the scatter. This may be due to the time lag of several months between crude purchases and product sales, and/or the limited population source of DOE's published price data.

Further, the market situation in 1979-1980 has restricted the number of bidders for military jet fuel contracts.

Several factors which could influence turbine fuel availability, costs, and bidding activity are seasonal markets, unleaded gasoline, conversion of power plants to coal, transportation situations, refinery equipment, and perhaps overly restrictive specifications on military turbine fuel (these factors are not meant to be listed in order of economic importance).

Extending endpoint specifications on turbine fuel could cause undue competition with diesel fuel for blending stocks, especially in the winter diesel market. Some refiners may need these stocks to lower the pour point of winter diesel.

FIGURE 8.5
NAPHTHA TYPE JET FUEL
WHOLESALE PRICES OVER LAST FIVE YEARS

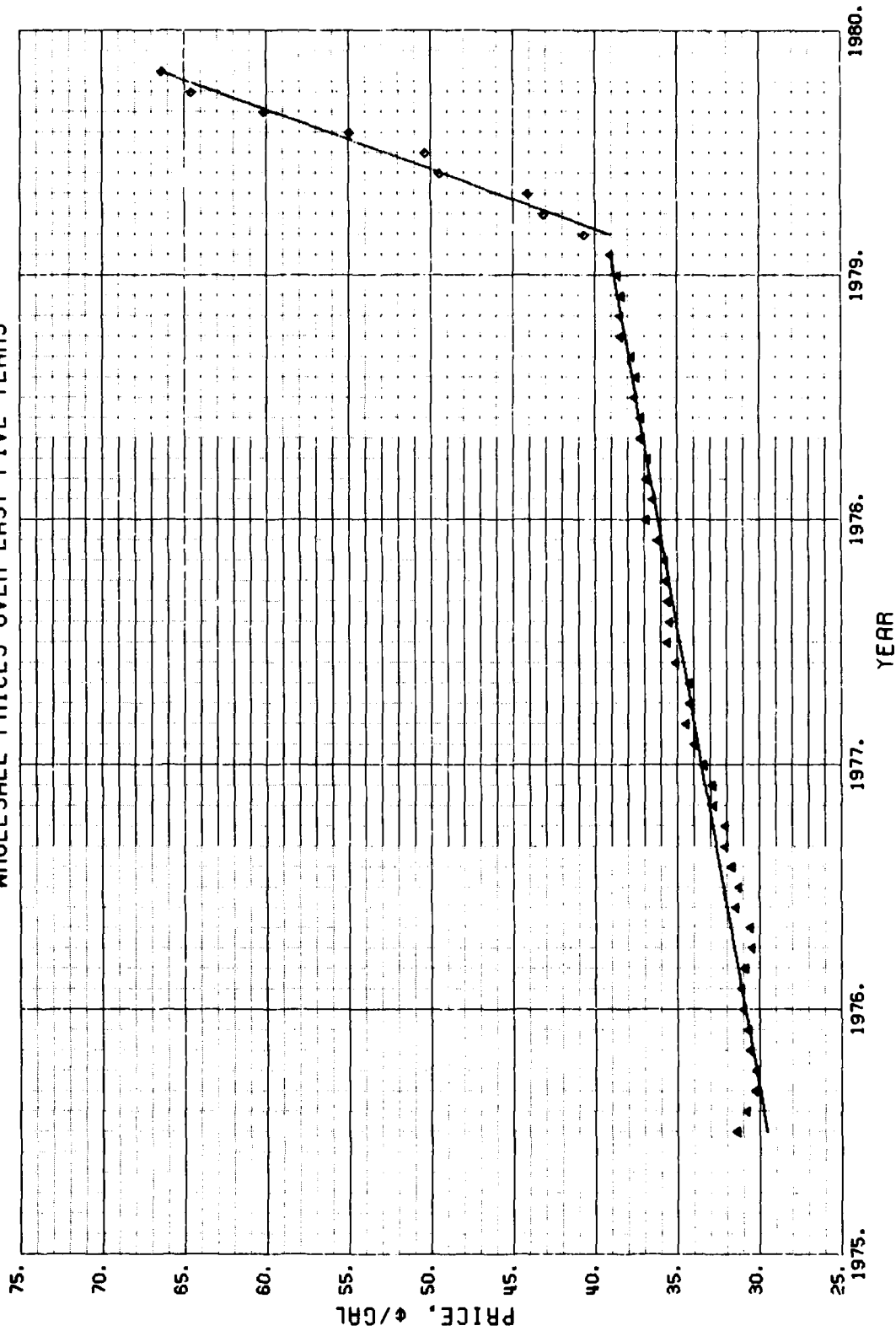


FIGURE 8.6
KEROSENE TYPE JET FUEL
WHOLESALE PRICES OVER LAST FIVE YEARS

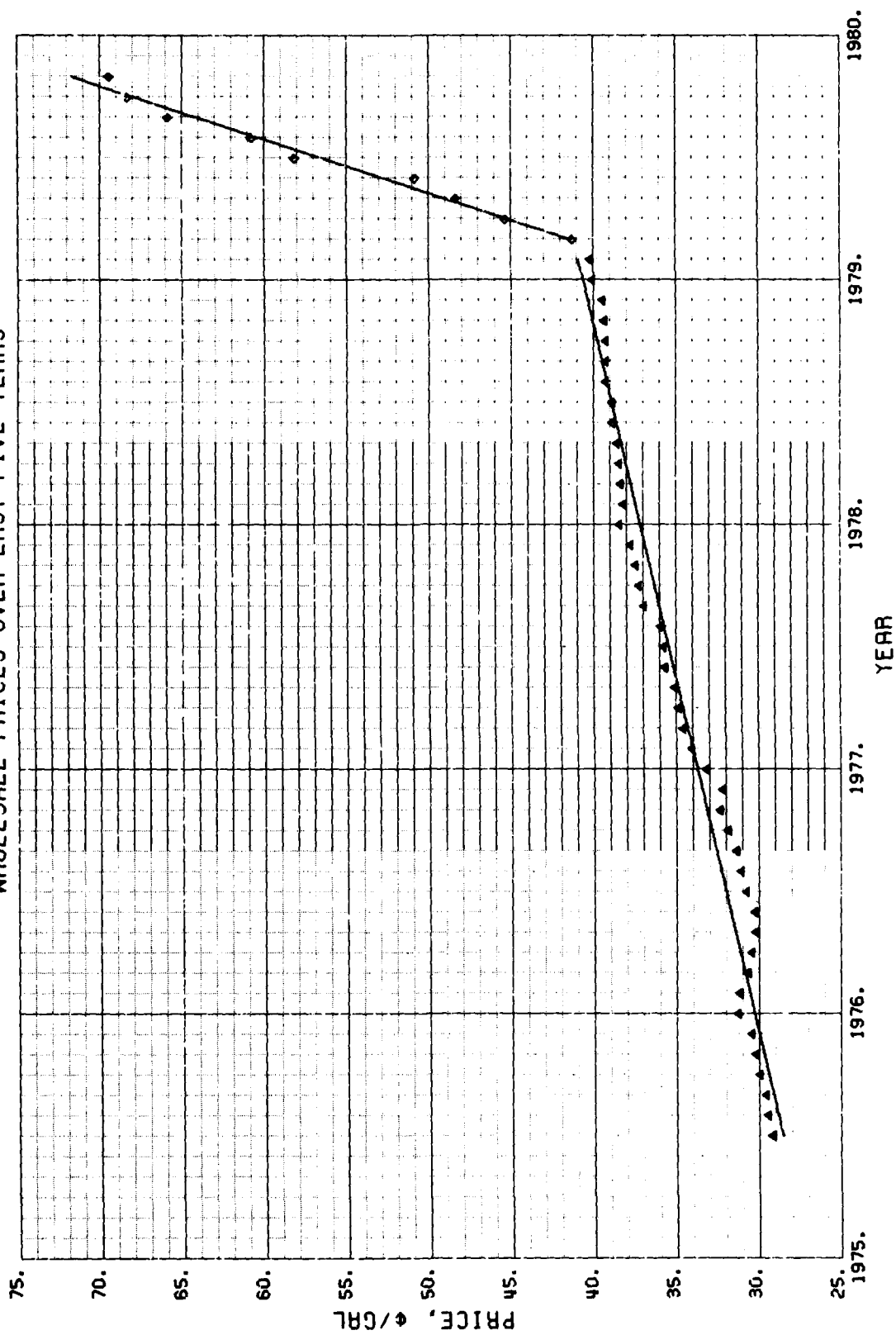


FIGURE 8.7
HEATING OIL

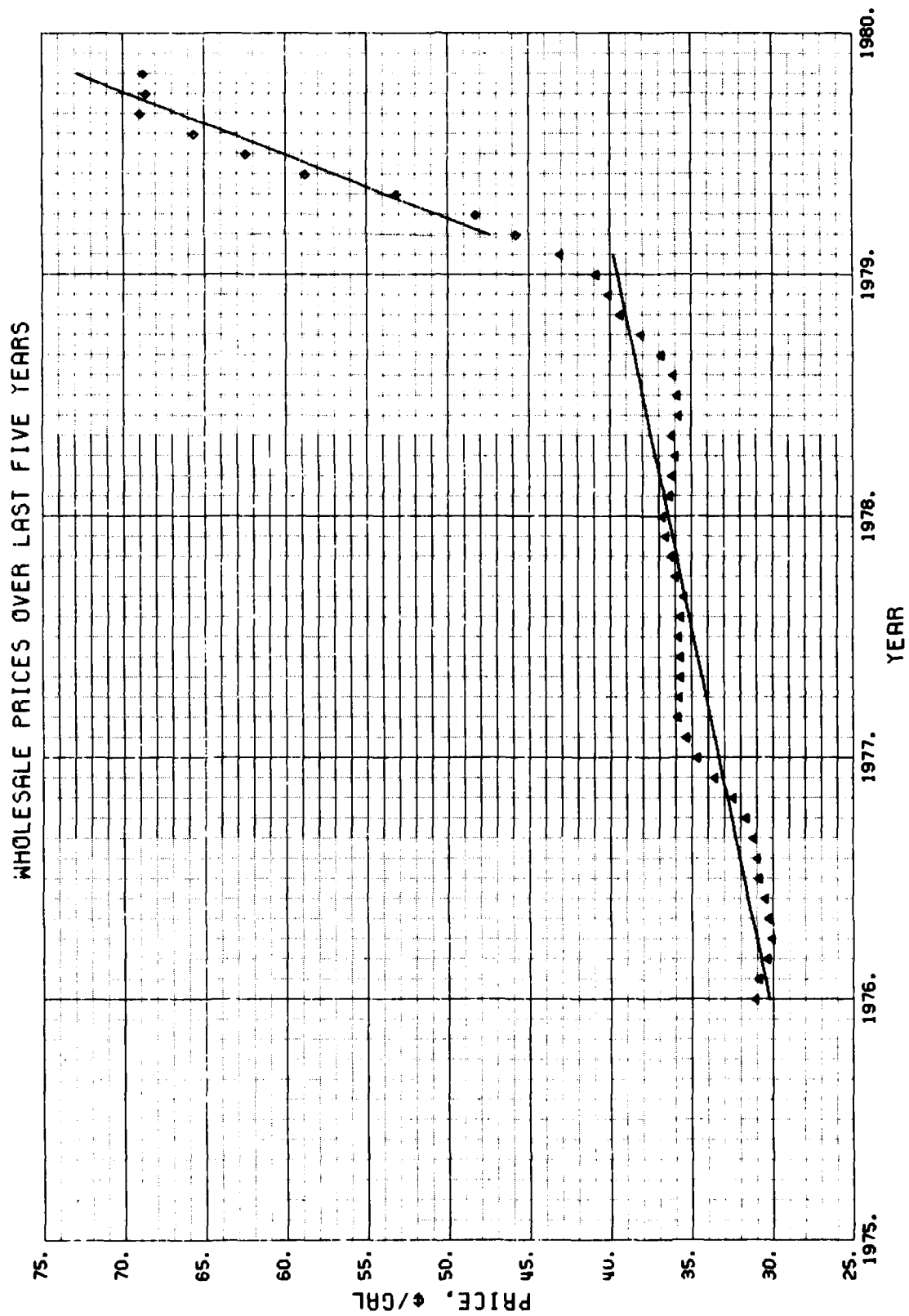


FIGURE 8.8
DIESEL FUEL

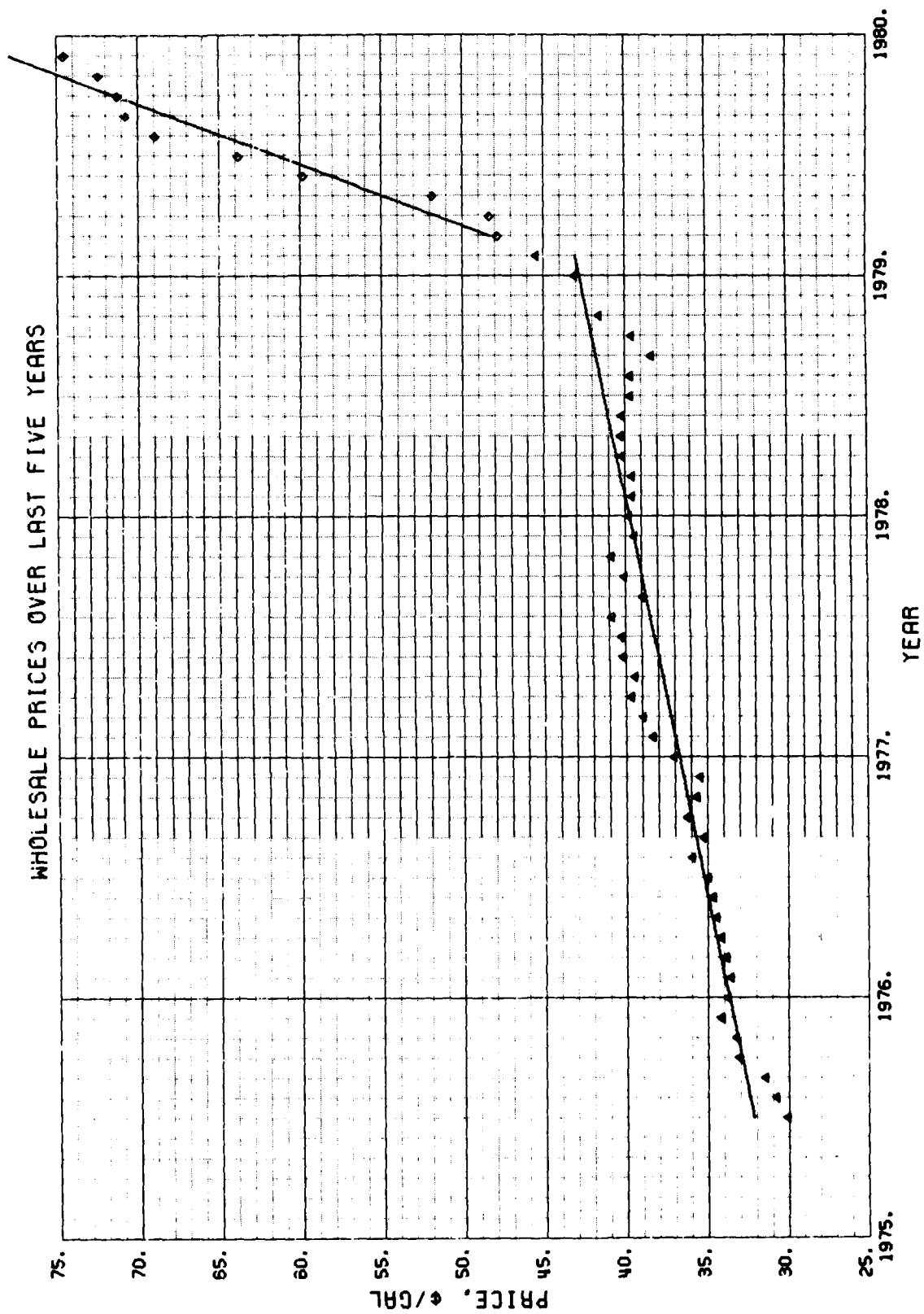


FIGURE 8.9

NAPHTHA TYPE JET FUEL MINUS CRUDE

WHOLESALE PRICE DIFFERENCE OVER LAST FIVE YEARS

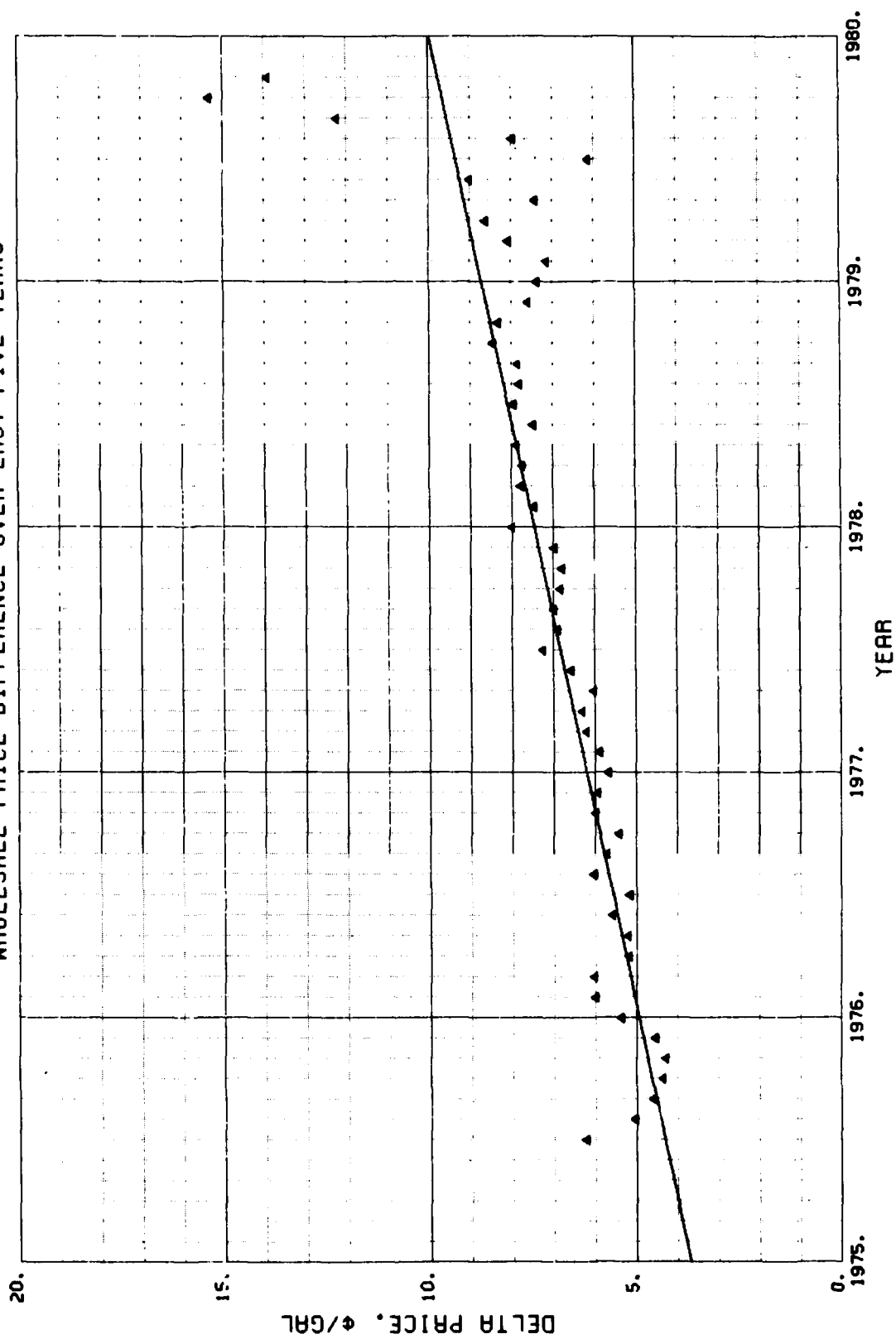


FIGURE 8.10
KEROSENE TYPE JET FUEL MINUS CRUDE
WHOLESALE PRICE DIFFERENCE OVER LAST FIVE YEARS

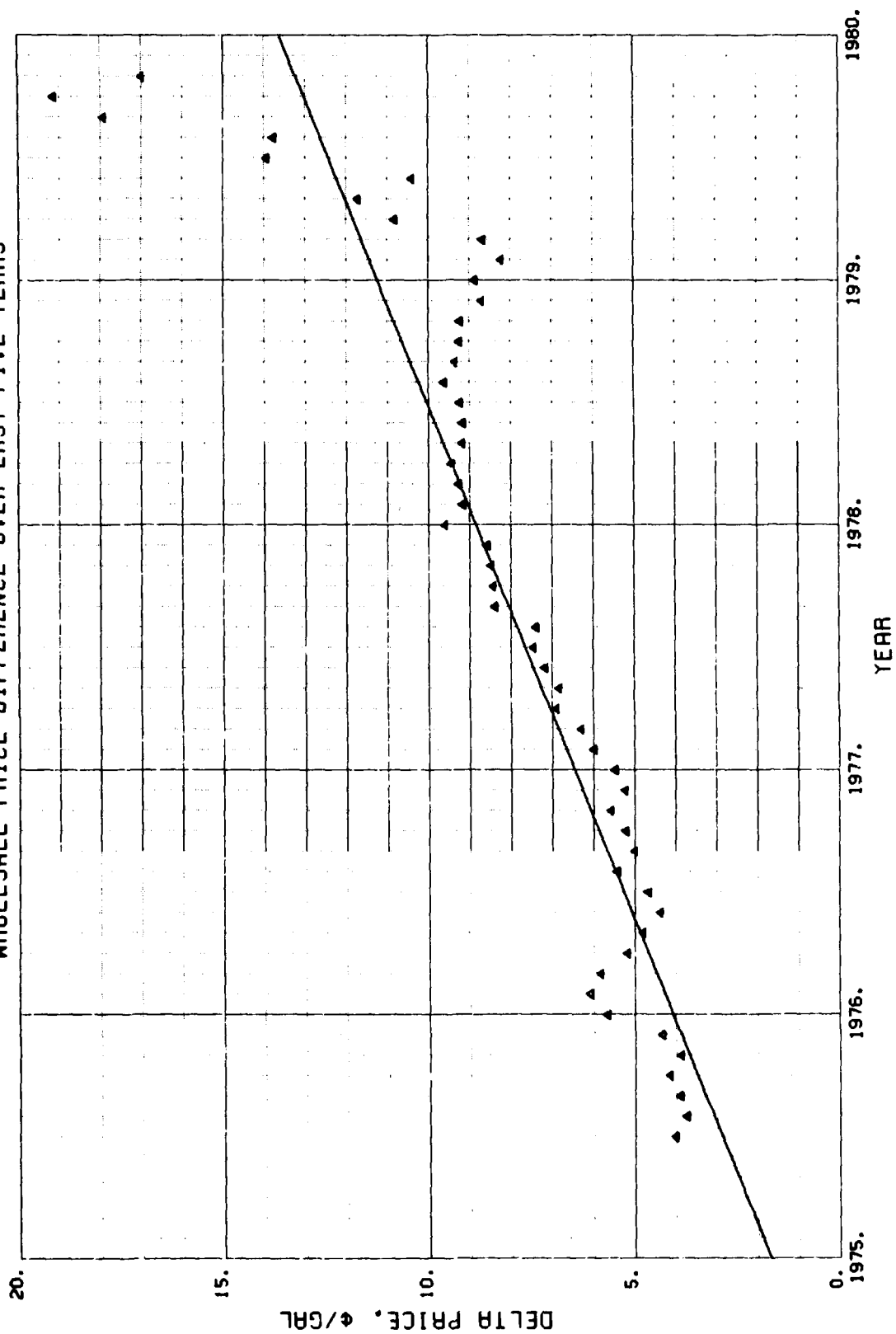


FIGURE 8.11
HEATING OIL MINUS CRUDE

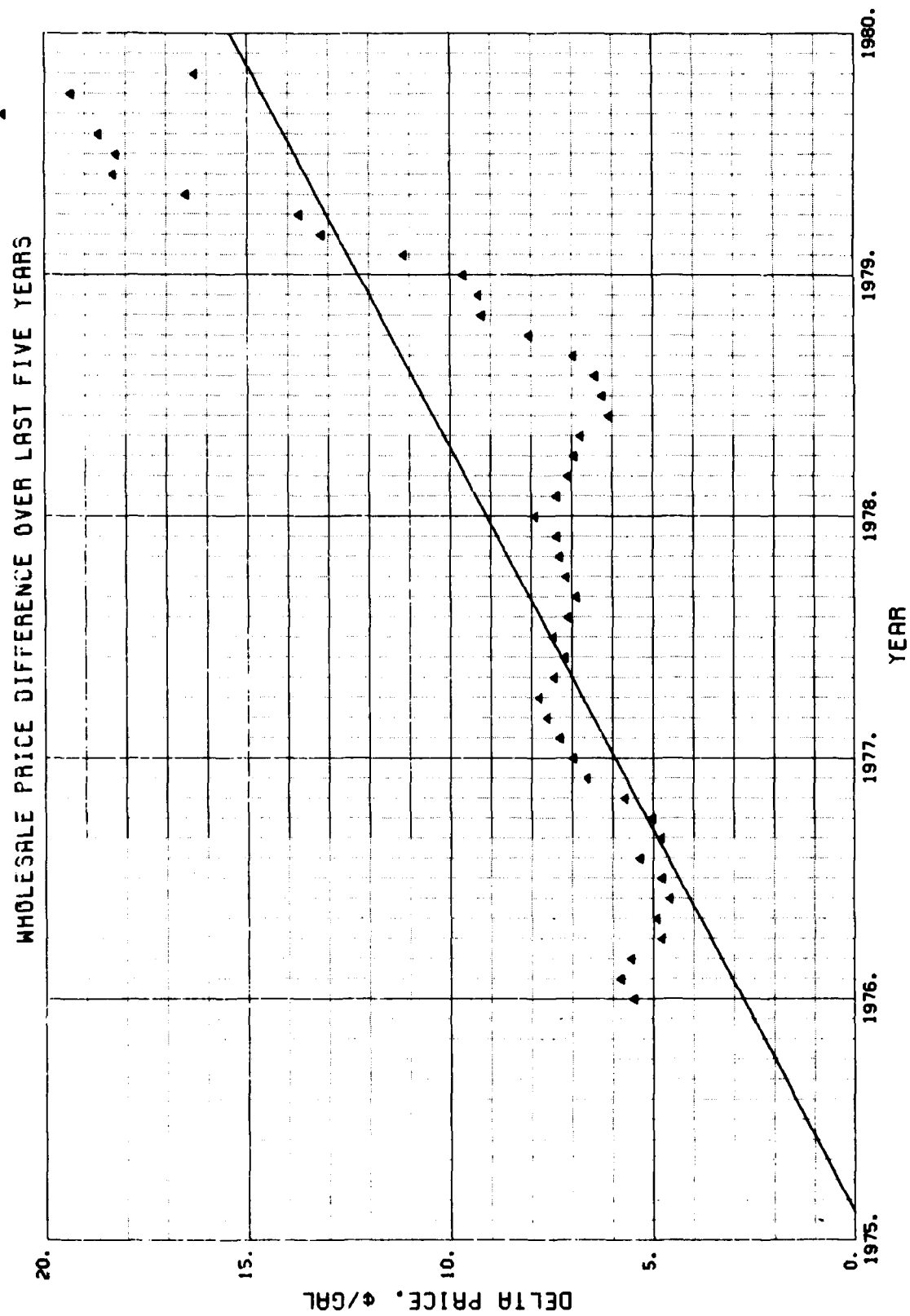
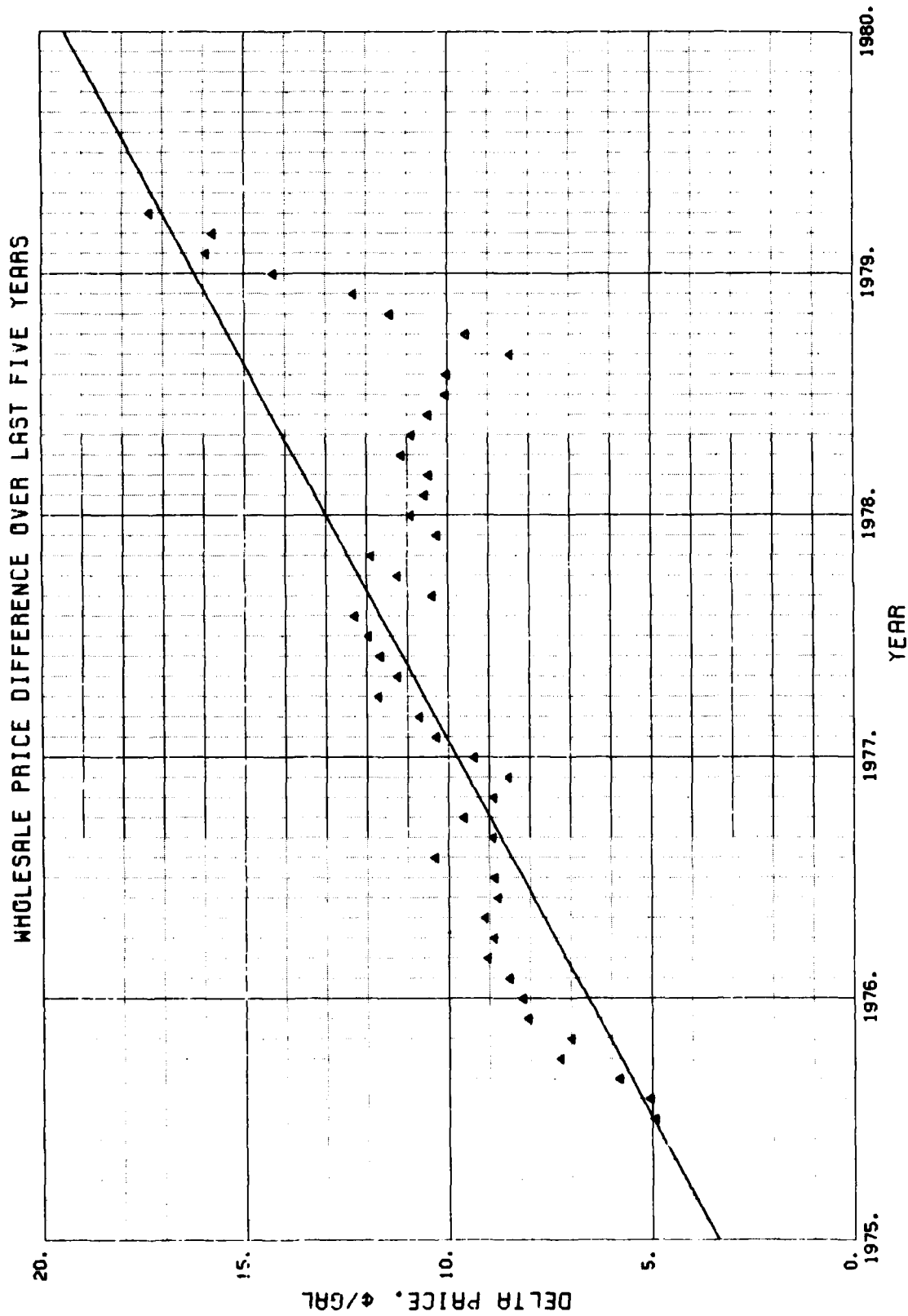


FIGURE 8.12
DIESEL MINUS CRUDE



The EPA mandated switch to unleaded gasoline has also resulted in a shortage of JP-4 type blending stocks. The reduction of lead in gasoline requires blending stocks with higher octanes than previously required. This has been accomplished by increasing the severity of the motor reformer operations, for the most part. Increasing reformer severity decreases reformer product yield. With tight supplies, low octane naphtha stocks which were formerly sent to JP-4 blending are now being sent to reformer units to make up for the loss in product yield due to the high severity operation.

Conversion of electric power plants to coal could increase distillate fuel supplies by releasing residual fuel oil now being burned. Most residual oil would be processed in delayed coking units, although there are a few Fluid Catalytic Cracking units which are being converted to residual feed processing. The liquid products for either of these units are unsuitable for blending into turbine fuel directly due to the olefin content, but they are potential feedstocks after hydrotreating. Not all refineries could handle such stocks and in many cases new equipment would be required for turbine fuel production. It must be emphasized that the effect due to coal conversion is long term and would have negligible immediate benefits.

As available crudes become heavier and higher in sulfur content, new refining equipment will be required. Small refineries who cannot afford such equipment will be forced to drop out of the turbine fuel market. Large refineries find it increasingly difficult to add new equipment due to environmental regulations and tight money supplies. Certainly, higher turbine fuel prices would be required to justify additional investments. Equipment required may range from naphtha desulfurizers to expensive turbine fuel aromatic saturation units.

One incentive toward inducing refineries to bid more on military turbine fuel would be to review the specifications and relax procedures. Turbine fuel destined for the commercial market may be shipped once the product tank meets specifications. A well mixed tank is sampled via side wall sample taps. Military procedures require multiple grab samples from the tank roof and retesting at each stage of shipment. Often much of the product is returned to the slops tank until pipelines are completely cleared. Clearly, it has become more costly to produce military turbine fuel than commercial turbine fuel.

One of the most severe specifications to meet is the water separation index, modified (WSIM). The problem with this test is that a corrosion inhibitor and an anti-static additive must be added to the fuel. Both additives are incompatible with the WSIM test. Thus, the WSIM of a product may be reduced from 90 to 40 or even 20 by the addition of the additives.

The WSIM of the final product may be brought up to spec with multiple water washings, but one would speculate that the water washings may remove the additives!

Thus, some refinery personnel prefer to not have to make military turbine fuel due to the costly procedures, small volume, and low price.

SECTION 9.

RELATED STUDIES

We have reviewed several studies related to the increased production of turbine fuel which have been reported in the last few years. Three are discussed below: An Exxon Study sponsored by ASTM, an Amoco Study presented at SAE, and a NASA Study. We have also briefly looked at the impact of the diesel automobile on the demand for diesel and turbine fuel.

Exxon Study

An ASTM sponsored study of jet fuel yield versus flash point was reported by E. R. Wieland and W. G. Dukek of Exxon Research and Engineering Company at the December 6, 1977 ASTM symposium, Dallas, Texas. Exxon has a computer program which simulates a crude oil distillation unit with up to fifteen crudes in the feed. The Exxon model predicts yields and physical properties of the various cuts produced.

Three cases were analyzed: a typical European refinery with a predominance of mid-Eastern crudes, a typical U.S. Gulf Coast refinery operating on a mixture of domestic and imported crudes, and a refinery running predominately Alaska North Slope crude.

The study was concerned with Jet A and Jet A-1 only. Concurrently, as the IBP was lowered to lower flash point, the FBP was raised to maintain freezing point and maximize volume produced. Smoke point, aromatics content, viscosity, and sulfur content were predicted for the various cuts.

One point they made, bears repeating. A refinery crude distillation unit will not produce as sharply a fractionated sidestream as one gets from a laboratory TBP distillation. Exxon applies a correction factor in their computer program for both front and back end volatility calculations to account for this variability. They point out that this correction can have a significant effect on the attainable flash and freezing points.

Results from Table 4 of their paper show that a 45.9 percent increase in jet fuel yield would theoretically result from a change in freezing point from -58°F to -40°F at a constant flash point of 100°F (Gulf Coast Refinery case). This yield increase corresponds to the yield increase of 40.7 percent for JP-8 given on page A-18 of this report.

Another point made in the Exxon paper is that many refineries do not segregate No. 1 fuel from jet fuel in product tankage. This reduces the opportunities to provide additional yield due to a specification change in one of the products. They also pointed out that the competing demands for "middle of the barrel" in many non-aviation applications make it unlikely that additional jet fuel volume will result from proposed specification changes.

Amoco Study

An economic study of producing motor diesel fuel rather than gasoline based on a linear programming model of a typical U.S. refinery was given at the September 1977 SAE meeting by T. O. Wagner of Amoco Oil Company. He found that at a constant volume of gasoline plus diesel, diesel fuel output could be increased from the present typical of 5 percent of crude to about 30 percent with no great change in processing

equipment. However, more crude is required as diesel volume is increased because diesel is heavier than gasoline. Also, in order to maintain company earnings, the diesel price would have to rise dramatically in relation to the gasoline price as the market emphasis is shifted from gasoline to diesel. Thus, increasing jet fuel volume by adding diesel type components will raise the cost of jet fuel relative to diesel.

NASA Study

In the NASA study, Francisco J. Flores of the NASA Lewis Research Center has used the Gordian Associates, Inc. Refinery Simulation model to predict jet fuel yields from two refinery types, one an East Coast refinery processing a 50/50 blend of Murban and Louisiana Delta crudes and the other a West Coast refinery processing a 50/50 mix of Alaska North Slope and Wilmington crudes.

The crude assay data stored in the Gordian model contain only five distillate cuts, light and heavy gasoline, light and heavy kerosene, and vacuum gas oil. Freeze point and smoke point data are given only for light and heavy kerosene (heavy kerosene is really a 525-650°F diesel cut). Manufactured keorsene cuts from fluid catalytic cracking and gas oil hydrocracking were added in the East Coast refinery case and hydrotreated kerosene was added in the West Coast refinery case. Therefore, freezing points and smoke points of jet fuel blending components were estimated and blended by generalized correlations. This means that the results are only good for suggesting trends and should not be used as an accurate yardstick for comparison.

From their Figures 9 and 10 which are of jet fuel yield versus final boiling point and freezing point, it appears that the Gordian model, with the constraints used in their study, could produce almost no jet fuel meeting current freezing point of -58°F (-50°C).

Impact of the Diesel Powered Automobile on Turbine Fuel

Figures 9.1-9.4 contain statistical data used to determine the approximate relationship of the private diesel automobiles to military aviation turbine fuel. General Motors is the only domestic manufacturer currently producing diesel powered automobiles. Figure 9.1 shows an extrapolation of GM's total passenger car output to 1983. The data for 1975-1978 was obtained from The World Almanac & Book of Facts, 1979 (Newspaper Enterprise Assoc, Inc., N.Y.). The GM diesel output was presented by GM president E. M. Estes at the 1979 Stockholders meeting in Detroit (Automotive News, p 55, June 4, 1979).

Figure 9.2 presents similar data for imported cars. Figure 9.3 shows the total number of cars in use in the U.S.A. Data for Figure 9.2 and 9.3 were obtained from Statistical Abstract of the United States, 1978 (U.S. Dept. of Commerce).

Utilizing the data from the three figures plus the estimates shown for percent of model year still in use in 1983, the total number of diesel cars in use in 1983 is estimated to be 3.1 million. This is 2.8 percent of total cars predicted in use in 1983 (112 million).

Assuming that diesel cars are driven 15,000 miles per year at 22 mpg, they would consume 50 million bbl/yr of diesel fuel in 1983. An extrapolation of distillate fuel oil production to 1983 as given in Figure 4.2 projects total distillate fuel oil to be 1,400 million bbl/yr. Thus, passenger car diesel consumption will be about 3.6 percent of total distillate fuel oil production in 1983.

FIGURE 9.1
GENERAL MOTORS PASSENGER CAR OUTPUT

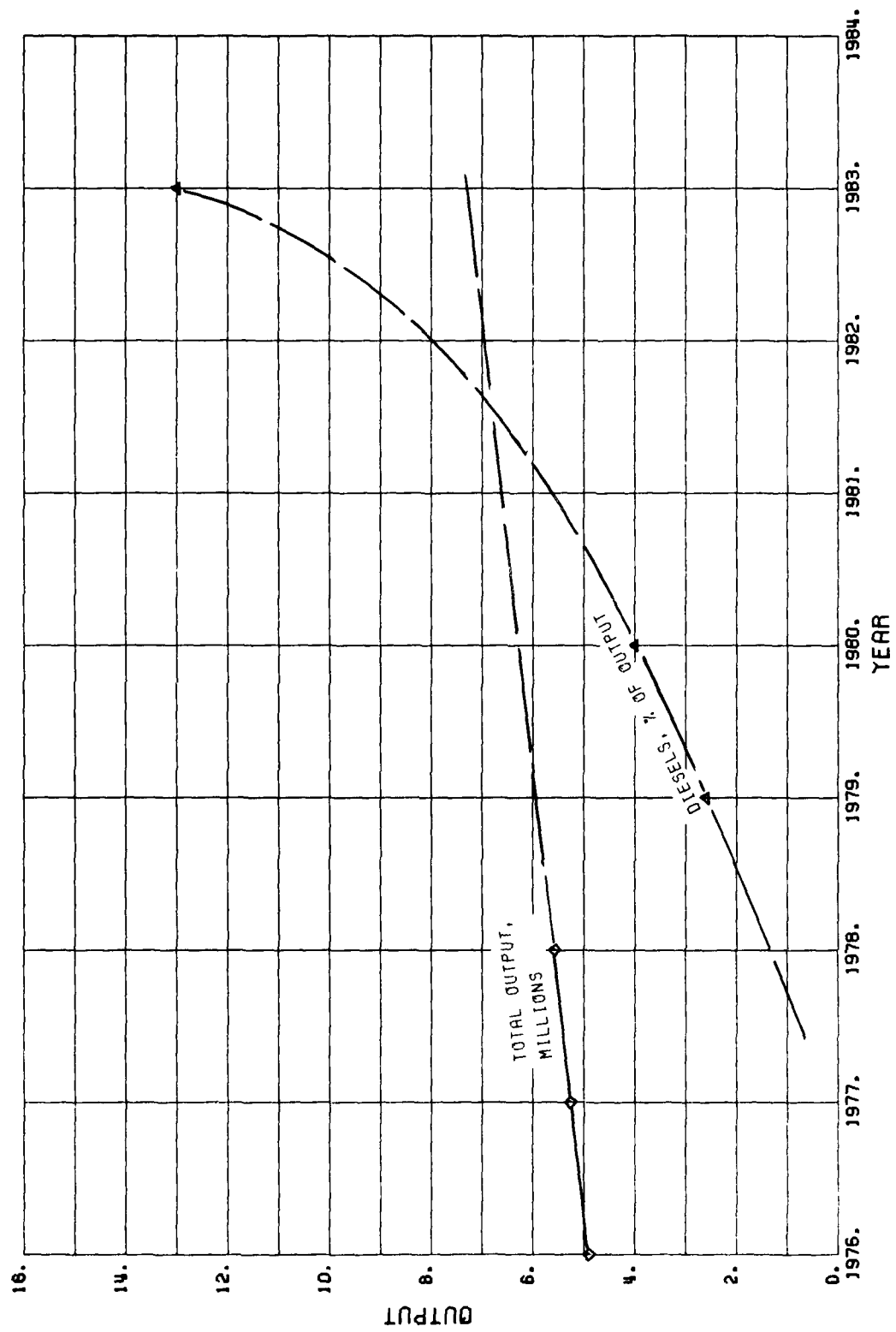


FIGURE 9.2
AUTOMOBILES IMPORTED IN U.S.A.

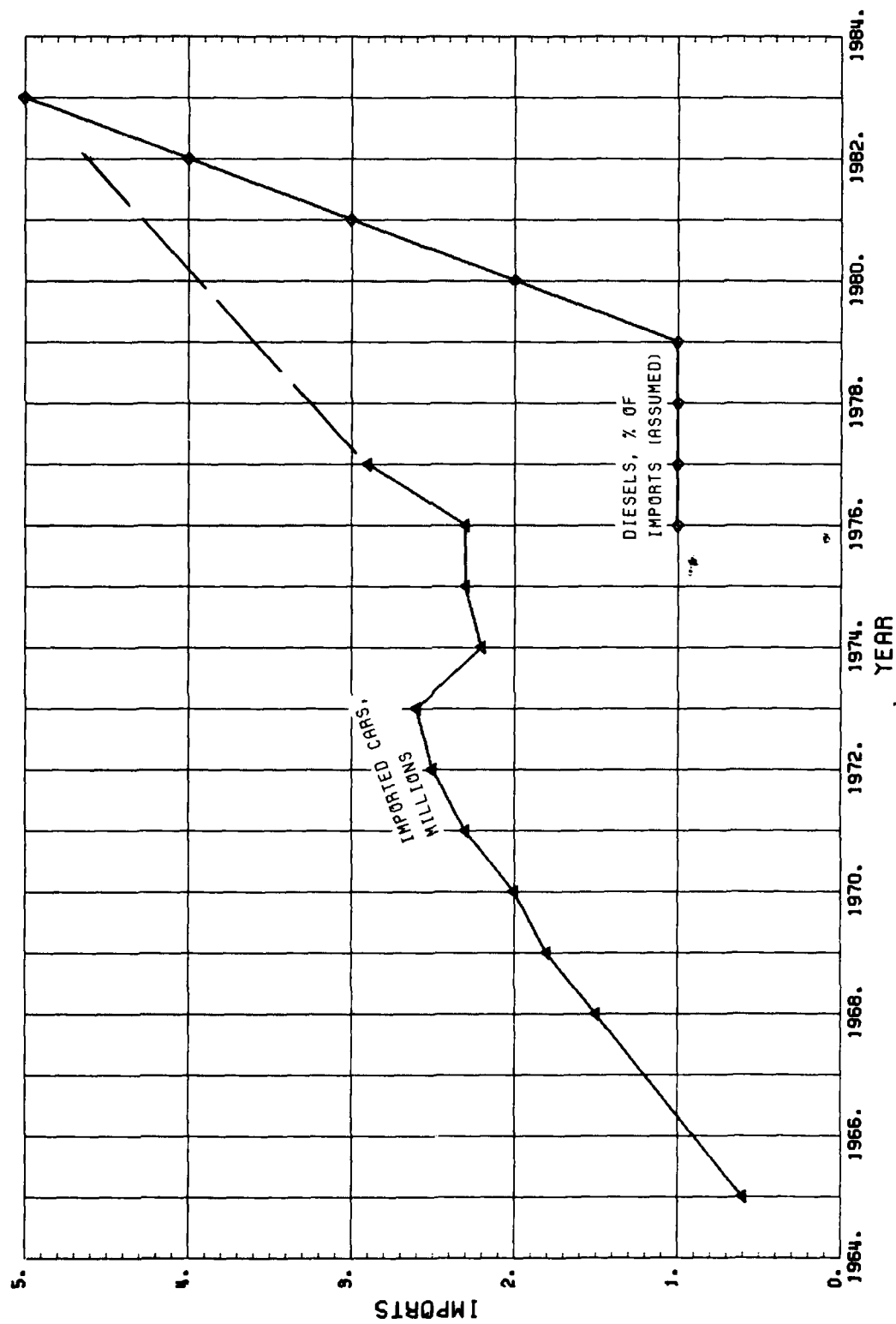


FIGURE 9.3
AUTOMOBILES IN USE IN U.S.A.

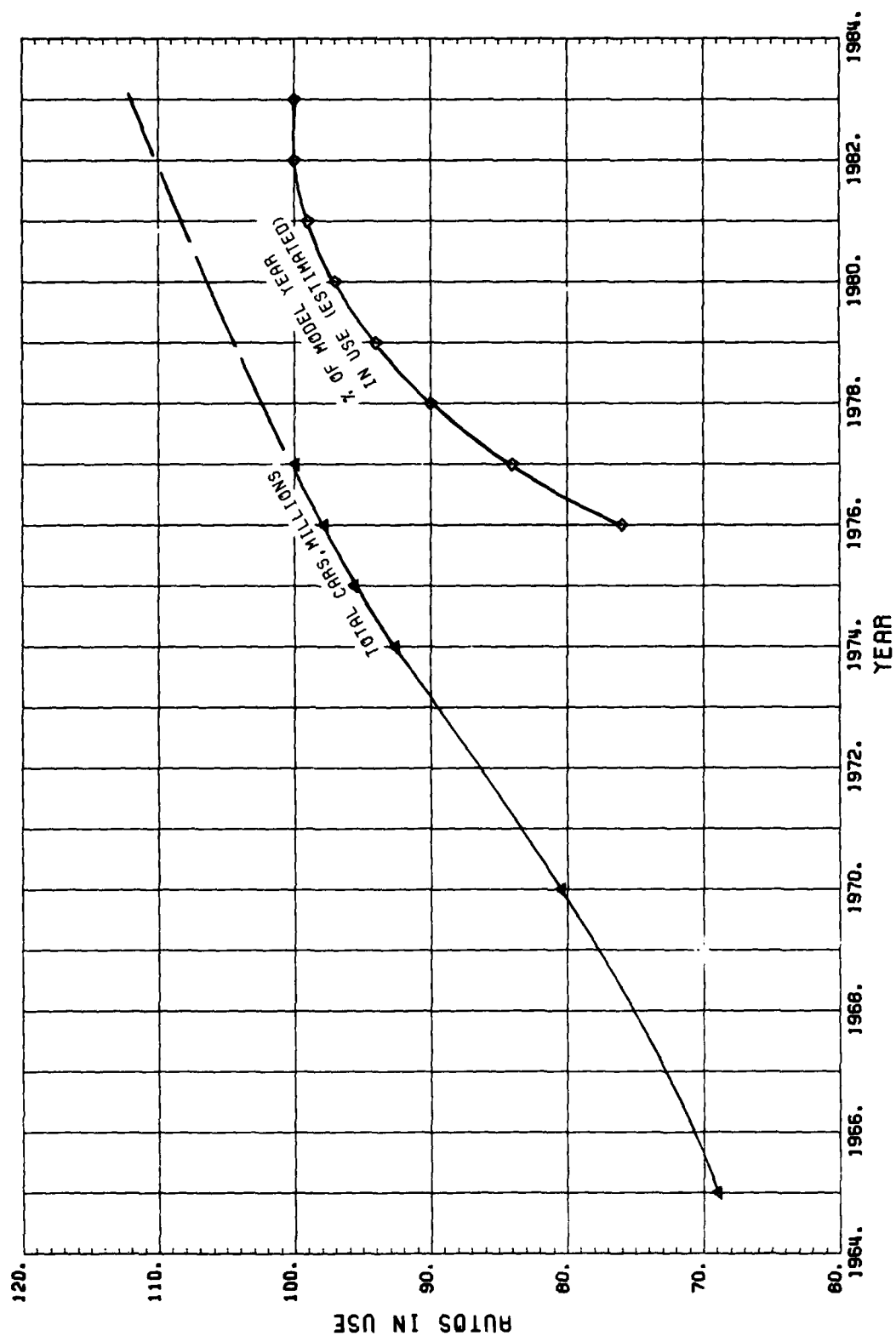


FIGURE 9.4
DOMESTIC DEMAND FOR NAPHTHA TYPE JET FUEL IN U.S.A.

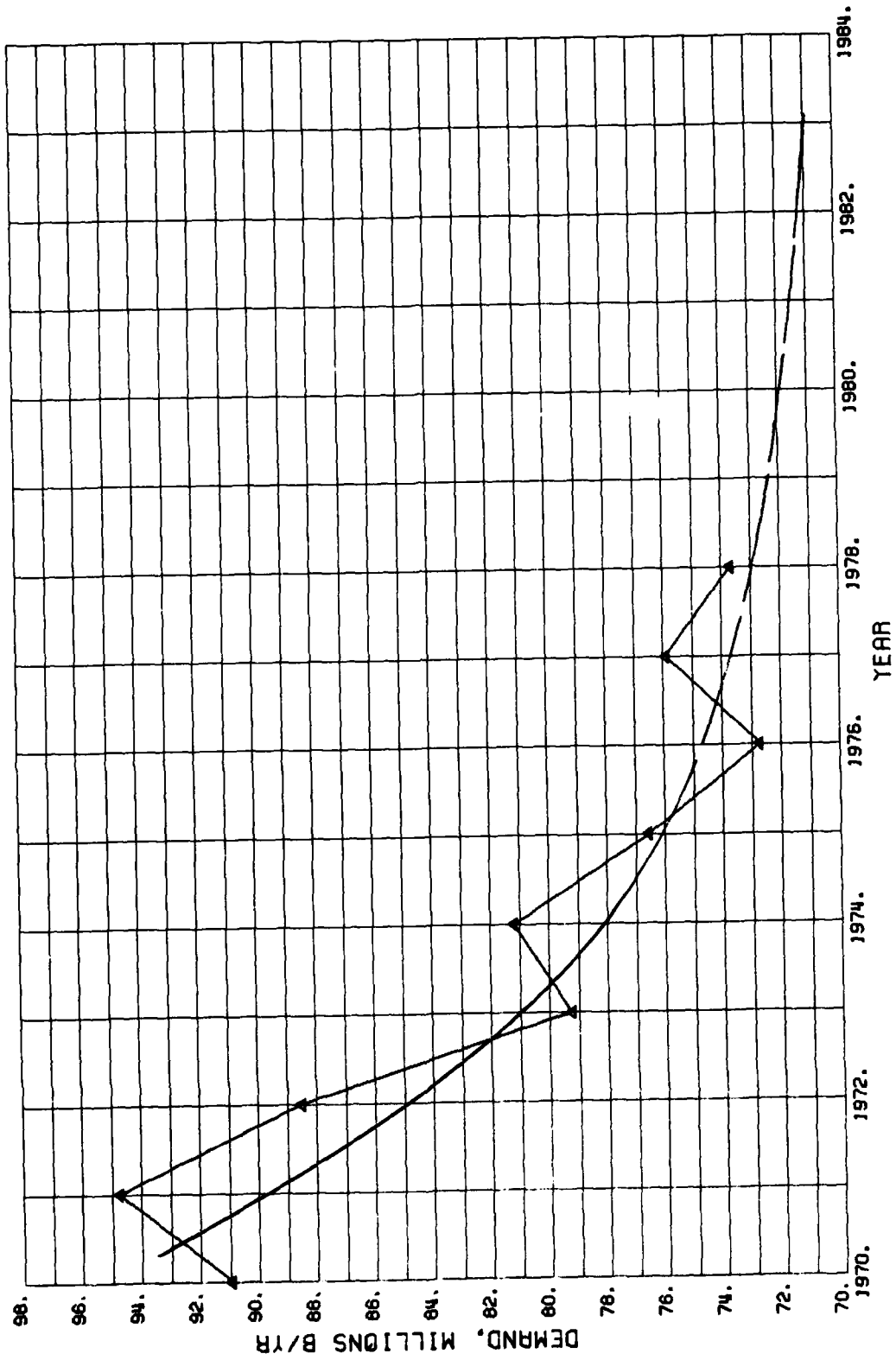


Figure 9.4 contains the domestic demand for naphtha type turbine fuel. A smooth curve extrapolation projects 71 million bbl/yr in 1983. Assuming that naphtha type turbine fuel is virtually all for the military, the automotive diesel fuel demand in 1983 will be 70 percent of the military turbine fuel demand.

This study projects a proposed increase of 9 percent of JP-4 under proposed specification changes. This would amount to about 6.4 million bbl/yr in 1983 of diesel type components. The projected automotive diesel demand in 1983 would be eight times the projected use of diesel in JP-4 in 1983.

Thus, automotive diesel demand will be small in 1983 compared to total distillate fuel oil produced, but very large compared to diesel fuel type components which would be used in JP-4 under the proposed specification changes.

SECTION 10.

SUGGESTED AREAS OF FUTURE STUDY

Two fundamental areas of this study should be improved if the Air Force decides that the study results are significantly interesting and warrant further study. Both of these proposals would be much more costly than the present study.

DATA BASE IMPROVEMENT

The first area of improvement would be to verify and improve the data base. Many of the crude assays used in the study are twenty years old. While crude oil from a given field does not change greatly over a long period of time, the outputs from different oil fields change as old wells are depleted and new ones brought into production. Thus, the data could be improved by analyzing turbine fuel yields from fresh crude samples. Also, more U. S. Crudes should be obtained to balance the study.

In addition, with the knowledge of the results of the present study, the required turbine fuel cuts could be made which would give much better final results. Physical properties of interest to the engine and airframe portions of the study could be obtained from the turbine fuel samples in addition to the properties normally obtained. These properties would include hydrogen content, low temperature viscosity, naphthalenes content, etc.

TURBINE FUEL SOURCES

The second area of potential refinement of the study would be to more accurately determine from which combinations of crudes that turbine fuels actually come. This is more subjective than the proposed laboratory study. A consulting firm could poll the industry to determine present and/or future sources of turbine fuel on a refinery-to-refinery basis.

These results would allow a more accurate averaging of yield increases and property changes. Such a mass of data could be accumulated that the use of computers would probably be required to complete the study results.

TEST FUEL SUPPLIES

An additional program could be added to the fuel supplies participant of any future study of turbine fuel specifications relaxation. This would be the supplying of actual turbine fuel meeting proposed specifications for engine and airframe testing. Production of such a test fuel could be obtained from traditional Air Force suppliers by bid invitations. This would likely be the most economical route to purchasing large volumes of test fuel.

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APPENDIX B

FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY
(ENGINE STUDY)

October 1, 1980

By

J. R. Grant

J. Herrin, Program Manager

PRATT & WHITNEY AIRCRAFT GROUP
Government Products Division

ABSTRACT

A program was conducted to assess the impact of several proposed relaxations of JP-4 and JP-8 fuel specifications on the performance and durability of gas turbine engines used in USAF aircraft. The proposed relaxations are concerned with specifications for final boiling point, freeze point, and smoke point. The effect of changes in these specified properties on other fuel properties, including fuel hydrogen content, aromatic content, viscosity, specific gravity, volatility, heat of combustion, and thermal stability was estimated. The impact of the proposed relaxations of JP-4 and JP-8 specifications on TF30, J57, and F100 engine performance was qualitatively discussed using available data in the literature. Thermal analyses were performed to determine the effect of the proposed specification relaxations on combustor liner and turbine airfoil temperatures for two USAF engines: the J57 and the F100. Results of these analyses were used to predict the corresponding impact on combustor and turbine life.

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NOMENCLATURE

<u>Symbol</u>	
A	Aromatic content, percent by volume
A_{IV}	Inlet vane projected surface area, in ²
A_{IVS}	Surface area of inlet vanes (pressure sides only), in ²
Ar	Argon
A_{REF}	Maximum combustor cross-sectional area, in ²
AMT	Accelerated mission test
atm	Pressure in atmospheres
BL	Beam length
Btu	British thermal units
C_p	Constant pressure specific heat
°C	Degrees Celsius
cm	Centimeter (1×10^{-2} meter)
cm ²	Area in square centimeters
CO	Carbon monoxide
CO ₂	Carbon dioxide
cs	Viscosity in centistokes
D_{crit}	Critical fuel droplet size
D_{cvt}	Maximum combustor width
d_q	Quenching distance
dia	Diameter
DFSC	Defense Fuel Supply Center
EL _{co}	Emission index of carbon monoxide
EL _{NO_x}	Emission index of nitrous oxides
EL _{HC}	Emission index of unburned hydrocarbons
ERBS	Experimental Reference Broad Specification
exp	Exponent
°F	Degrees Fahrenheit
F_{c-IV}	Radiation shape factor from radiating gases to inlet vanes
F_N	Fuel nozzle flow number
f/a	Fuel to air ratio by mass
FBP	Final boiling point, °F
FO	Fuel oil
ft ²	Area in square feet
G	API gravity
$\mu\text{gm/cm}^2 \text{ hr}$	Coke formation rate
g/kg	Grams per kilogram
H	Hydrogen content, percent by weight
HC	Hydrocarbon
H/C	Hydrogen to carbon ratio
h_{c-IV}	Average convective heat transfer coefficient on the pressure side of the inlet vanes
hr	Time in hours
IBP	Initial boiling point, °F

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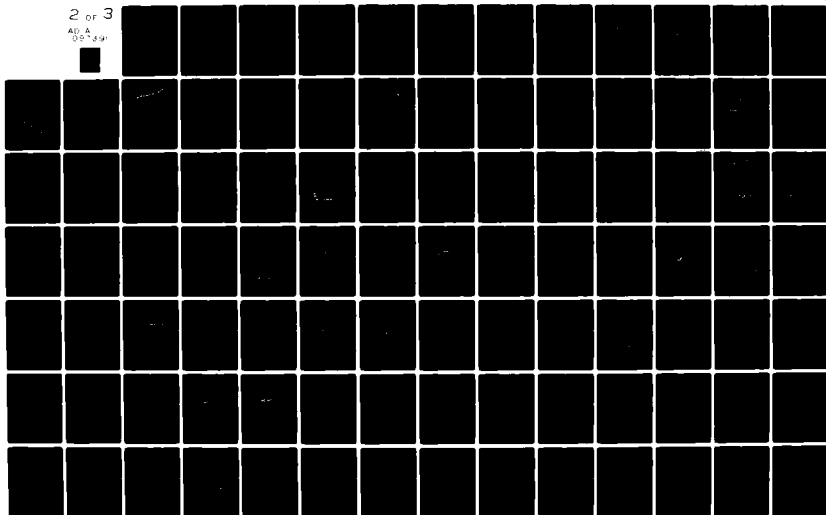
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JFTOT	Jet fuel thermal oxidation test
K	Constant for fuel nozzle configuration
°K	Degrees kelvin, °C + 273
k	Kilo (1×10^3)
k	Thermal conductivity
kcal/mole	Mean activation energy of fuel
kg/sec	Flowrate, kilograms per second
L	Luminosity
LCC	Life-cycle cost
LCF	Low-cycle fatigue
LPT	Liner temperature parameter
lbma	Weight of air, pound mass
lbmf	Weight of fuel, pound mass
ln	Natural logarithm
m	Meter
mm	Millimeter (1×10^{-3} meters)
μ m	Micrometer (1×10^{-6} meters)
ms	Milliseconds (1×10^{-3} sec)
mm Hg	Pressure in millimeters of mercury
mj	Millijoule
MIE	Minimum ignition energy, mj
MPa	Pressure in megapascals
N	Nitrogen content, percent by weight
N2	High-pressure rotor speed, rpm
N ₂	Nitrogen
Na	Naphthalene content, percent by weight
N/M ²	Pressure in Newtons/squaremeter
NO	Nitrous oxide
NO _x	Nitrogen oxides
Ol	Olefin content, percent by weight
O ₂	Oxygen
P	Combustor pressure, psi
psia	Pressure in pounds per square in. absolute
psig	Pressure in pounds per square in. gage (psia - 14.7)
P _{IN}	Combustor inlet pressure, psi
Δ Pa	Pressure drop across an airblast fuel nozzle
q	Radiant heat transfer
Q _{co}	Lower heating value of carbon monoxide
Q _{c1}	Convective heat transfer from hot flame gases to louver wall
Q _{c2}	Convective heat transfer from louver wall to shroud air flow
Q _{fuel}	Lower heating value of fuel
Q _{R1}	Radiative heat transfer from hot flame gases to louver wall
Q _{R2}	Radiative heat transfer from louver wall to shroud wall
Q _{UHC}	Lower heating value of unburned hydrocarbons
°R	Degrees Rankine, °F + 459.69
RVP	Reid vapor pressure

S_L	Laminar flame speed
S	Sulfur content, percent by weight
SG	Specific gravity of fuel
SG_b	Specific gravity of base fuel
SP	Smoke point of fuel
SP_b	Smoke point of base fuel
$SLTO$	Sea level take-off
SMD	Droplet size, Sauter Mean Diameter
T	Volume recovery temperature
T_{exit}	Combustor exit temperature
T_g	Hot gas or flame temperature
T_l	Flame or liner wall temperature
T_{in}	Combustor, inlet temperature
T_{iv}	Inlet vane temperature
T_L	Liner temperature
$(T_L)_b$	Average liner temperature with base fuel
T_w	Liner wall temperature
TBP	Temperature breakpoint in stability
TMP	Trimethylpentane
T_3	Combustor inlet temperature
UHC	Unburned hydrocarbons
\bar{V}	Average distillation temperature
wgt	Weight
W_a	Mass flowrate of air
W_f	Mass flowrate of fuel
α_f	Absorptivity of flame
α_g	Absorptivity of hot gases
β	A variable defined by equation 28
σ_{sb}	Stephan-Boltzmann constant, $0.1713 \times 10^{-8} \text{ B/ft}^2\text{-hr-}^\circ\text{R}$
σ_f	Fuel surface tension
ν	Viscosity, centistokes
ν_b	Viscosity of base fuel, centistokes
ϵ_g	Emissivity of hot gases
ϵ_{iv}	Emissivity of inlet vanes (assumed to be 0.8)
ϵ_w	Emissivity of liner wall
η_c	Combustion efficiency
ρ	Density
ρ_a	Density of air
ρ_f	Density of fuel
θ	Combustor loading parameter
μ	1×10^{-6}
Δ	Denotes a change in a variable
ϕ	Equivalence ratio, fuel-air ratio divided by stoichiometric fuel-air ratio

SECTION I

INTRODUCTION

PROBLEM DEFINITION

Current specifications for gas turbine fuels were instituted during an era when the availability of petroleum products was unrestricted, and the costs of fuels constituted a small percentage of the total life cycle cost (LCC) of aircraft systems. Since the 1973 oil embargo, however, the supply of petroleum has become increasingly dependent on geopolitical considerations, and per-gallon costs have more than tripled for civilian and military consumers. Fuel procurements for military usage have actually encountered difficulties in obtaining desired quantities even though conservation efforts have reduced consumption from 1972 levels. As a result of these trends, there has been increasing interest in recent years to broaden or relax current military fuel specifications, with the intent of increasing fuel availability while potentially reducing fuel costs.

The most significant potential increases in the availability of military jet fuels obtainable are predicated on the relaxation of the specification requirements for freeze point, final boiling point, and combustion properties (smoke point and aromatic hydrocarbon content). Bonner and Moore (Reference 1) suggest that relaxing the freeze point by 15°F and the final boiling point by 25°F will increase the availability of JP-4 by 24.3% over the 1975 production levels. Moreover, similar changes for JP-8 fuels, with the additional relaxation of combustion properties, increases the availability of this fuel by 27.6%.

Although broadening of fuel specifications may increase the supply of jet fuels, it may also hamper engine performance and component durability. It is to this problem that the present program is directed. Specifically, the purpose of this two-phase program is to assess the impact of broadened-specification fuels on gas turbine engines used in USAF aircraft. This report presents the findings of the effort conducted under Phase I of this program.

BACKGROUND AND OVERALL APPROACH

In general, the effect of broadened- or relaxed-specification fuels on gas turbine operation characteristics is expected to encompass several performance and durability-related parameters. These parameters include ignition, combustion efficiency, emissions, thermal loads, burner exit temperature distribution, erosion, and coking of fuel systems. Although the exact composition of a suitable broadened-specification fuel is not known at this time, several proposed relaxations of current JP-4 and JP-8 fuel specifications are considered in this study, and are listed below:

- JP-4: Freeze point 14°F increase
Final boiling point 25°F increase
- JP-8: Freeze point 18°F increase
Final boiling point 25°F increase
Smoke point 2mm decrease.

The program is functionally divided into two phases. Phase I of the program is concerned with the impact of broadened-specification fuels in general, and the impact of the specified changes in fuel properties listed above. The specific objectives of the Phase I study are threefold as follows:

- To qualitatively assess the impact of broadened-specification fuels on gas turbine operational characteristics using available data in the literature

- To quantitatively assess the impact of the specified changes in JP-4 and JP-8 fuel properties on combustor liner and turbine airfoil temperatures
- To predict the life degradation of the combustor and turbine components corresponding to the specified changes in fuel properties.

Phase II of this program will be directed towards integrating the findings of the Phase I study into a life cycle cost study.

PHASE I SCOPE AND OVERVIEW

The specified changes in JP-4 and JP-8 fuel properties listed were used to imply changes in fuel properties which directly affect engine performance and durability. This was accomplished through the use of various interproperty correlations and an assumed change in JP-4 and JP-8 distillation characteristics. The assumptions and methods used in developing these correlations, and the corresponding estimates of fuel property changes, are given in Section II of this report.

Section III of this report discusses the findings of the literature survey conducted to determine the effect of broadened-specification fuels on engine performance and durability. Discussions included in this section are primarily qualitative in nature and deal with the effects of both chemical and physical fuel properties on the operation characteristics of three USAF engines: the J57, TF30, and F100. When possible, however, quantitative estimates of the impact of the proposed relaxations of JP-4 and JP-8 fuel specifications on the performance of these engines are given.

Thermal analyses were conducted to determine the impact of the proposed changes in JP-4 and JP-8 fuel properties on combustor liner and turbine airfoil temperatures for two USAF engines: the F100-PW-100 and the J57-59W. These analyses are in the form of analytical studies utilizing the information contained in Sections II and III. No experimental data was generated. The methodology used in the analyses and the results are presented in Section IV.

The estimated increases in combustor liner and turbine airfoil temperatures found in Section IV were used in conjunction with existing information to predict the resulting decrease in combustor and turbine life. These findings are discussed in Section V of this report.

Section VI provides a summary of efforts expended under Phase I and the conclusions regarding these efforts. Section VII details the recommendations for future fuel-related and combustor design studies.

SECTION II

FUEL CHARACTERIZATION AND INTERPROPERTY CORRELATIONS

The proposed relaxation of JP-4 and JP-8 fuel specifications presented in Table 1 are given in terms of final boiling point, freeze point, and smoke point only; however, changes in these three properties would be expected to result in concomitant changes in other fuel properties as well. To identify those properties affected, and to quantify the magnitude of the changes, a fuel characterization study was conducted. Primary among those fuel properties which are affected by changes in the final boiling point, freeze point and smoke point and which also exhibit a significant impact on gas turbine combustor and turbine operation include:

- Hydrogen content
- Aromatic content
- Viscosity
- Specific gravity
- Volatility
- Heat of combustion
- Thermal stability

TABLE 1. PROPOSED RELAXATIONS
OF JP-4 AND JP-8 FUEL
SPECIFICATIONS

JP-4	14°F	Increase in Freeze Point
	25°F	Increase in Final Boiling Point
JP-8	18°F	Increase in Freeze Point
	25°F	Increase in Final Boiling Point
	2mm	Decrease in Smoke Point

Each of these fuel properties will be discussed in the section and, where applicable, changes in these properties due to the proposed relaxations of final boiling point, freeze point, and smoke point are estimated through the use of interproperty correlations. In addition, changes in fuel distillation characteristics are inferred from the specified change in final boiling point and are used to estimate corresponding changes in fuel viscosity, specific gravity, and volatility.

All changes in fuel properties presented in this section are given relative to base JP-4 and JP-8 fuels. Base fuel properties used in this study are presented in Table 2, along with their corresponding military specifications. The base JP-4 represents an average fuel, based on a statistical summary of physical properties of JP-4 fuels procured by the Defense Fuel Supply Center during 1975 (Reference 2). Since there is relatively little jet fuel made to the JP-8 specifications in the world, an average JP-8 fuel is of little significance and, instead, a typical of JP-8 fuel is used.

As shown in Table 2, fuel properties of the base JP-4 and JP-8 fuels are, for the most part, far from approaching their respective current specification limits. For example, the final boiling points for JP-4 and JP-8 base fuels are 61°F and 44°F below specification limits, respectively. Since the proposed increase in final boiling point is only 25°F, the effect of a change from a base fuel final boiling point value to a proposed specification limit is due primarily to the difference between base and current specification values. Therefore, changes in fuel properties can be considered with regard to two cases: Case 1 — corresponding to the change from a base JP-4 or JP-8 fuel value to current specification limits, and Case 2 — corresponding to a change from a

base JP-4 or JP-8 fuel value to the proposed specification limits. In this section, estimated changes in hydrogen content are given for both Case 1 and Case 2, and are used later in Section IV of this report to illustrate the resultant impact on liner temperatures. Changes in all other fuel properties are given with regard to Case 2 only.

It should be emphasized that interproperty correlations presented in this section should be used with caution. A given jet fuel is a blend of a large number of constituents which may originate from several different crude oils. Thus, a large amount of data scatter is normally incurred when attempting to correlate one fuel property with another. The correlations are intended to be used to establish trends in fuel property relationships or, as mentioned above, to infer a change in a fuel property relative to a base value, and should only be used for that purpose.

TABLE 2. JP-4 AND JP-8 FUEL PROPERTIES

	JP-4		JP-8	
	Average ¹ Value	Specification ² Requirement	Typical ³ Value	Specification ⁴ Requirement
Initial Boiling Point, °F	141	—	286	
10% Distillation Point, °F	211	—	340	401 (max)
20% Distillation Point, °F	233	310 (max)	364	
50% Distillation Point, °F	295	374 (max)	410	
90% Distillation Point, °F	403	473 (max)	480	
Final Boiling Point, °F	457	518	506	550 (max)
Freeze Point, °F	—	-72 (max)	—	-58 (max)
Heat/Comb., Btu/lbm	18,729	18,400 (min)	—	18,400 (min)
Smoke Point, mm	28.1	—	25	25 (min)
Aromatics, % Vol	10.9	25 (max)	13.1	25 (max)
Gravity, ° API	53.9	45-57	44.9 ⁵	39-51

¹From Reference 2.

²Mil Spec 5624K.

³Values for a particular JP-8 fuel from Reference 3, p. 30.

⁴Mil Spec 83133.

⁵Calculated from specific gravity.

HYDROGEN CONTENT

Hydrogen content provides a measure of the combustion quality of the fuel and is expressed in terms of the weight percent of hydrogen in a fuel. It has been found to correlate reasonably well with some combustor operating characteristics, particularly liner metal temperature and radiant heat flux. Numerous studies have shown that a decrease in fuel hydrogen content results in higher radiant heat loads to combustor liners and, therefore, higher liner temperatures.

In this study, changes in hydrogen content resulting from the relaxed JP-4 and JP-8 fuel specifications were estimated using a correlation between smoke point (SP) and hydrogen content (H). Two correlations of this type were considered, and are shown below:

$$SP = \exp \left(\frac{H - 7.61}{2} \right) \quad (\text{from Reference 4}) \quad (1)$$

$$SP = \exp \left(\frac{H - 4.54}{3.03} \right) \quad (\text{from Reference 5}) \quad (2)$$

Equation 2 indicates a slightly greater dependence of hydrogen content on smoke point, and was used in this study. When determining a change in hydrogen content from smoke point, not only was the specified smoke point change of -2.0 mm (for JP-8 only) considered, but also the change in smoke point implied by the specified changes in final boiling point and freeze point.

Data correlations between FBP and freeze point reported by Union Oil Company (Reference 4) are shown in Figures 1 and 2. These correlations were developed from analyses of crude assays for African, Middle East, and several miscellaneous crudes. For the specified changes in freeze point of +14°F and +18°F, the data in Figures 1 and 2 predict a much greater change in FBP (in the approximate range of 35 to 70°F) than the specified change of +25°F. Figure 3 shows a correlation between FBP and freeze point developed from average jet fuel data (Reference 6). This correlation predicts a change in FBP of +23°F and +29°F for the specified changes in freeze point of +14°F and +18°F, respectively. The obvious discrepancy between the correlations shown in Figures 1 and 2 and that shown in Figure 3 serves to illustrate the fact that the various processes involved in producing a jet fuel blend from a variety of crude oils may change the interrelationships between various fuel properties. For the present study, it was assumed that in an end-product jet fuel, freeze point will follow from the final boiling point. Therefore, implied changes in the smoke point due to changes in the freeze point were neglected.

Implied changes in smoke point resulting from a change in the final boiling point were determined using the data shown in Figure 4. This data was also obtained from a Union Oil analysis of crude assays (Reference 4). A linear regression analysis of the data of Figure 4 yields the following equation:

$$SP = 35.26 - 0.024 \text{ FBP} \quad (3)$$

As is shown, there exists considerable scatter in the data; therefore, Equation II-3 was used only to imply a change in SP resulting from a change in FBP, and the requisite equation becomes

$$\Delta SP = -0.024 (\Delta \text{FBP}) \quad (4)$$

Equation 4 was used to determine a change in smoke point resulting from both Case 1 and Case 2 changes in FBP (found from Tables 1 and 2). The results are shown in Table 3 for both JP-4 and JP-8 fuels. The change in SP for JP-8 corresponding to a ΔFBP of 69°F was found to be 1.7 mm; therefore, the contract-specified change of 2.0 mm dominates.

The changes in smoke point presented in Table 3 were used to determine a change in fuel hydrogen content (ΔH) by rearranging Equation 2 as

$$\Delta H = 3.03 \ln \left(\frac{SP}{SP_b} \right) \quad (5)$$

where S_b indicates a base JP-4 or JP-8 fuel smoke point. The change in hydrogen content for the JP-4 and JP-8 fuels are shown in Table 4 for both Case 1 and Case 2.

AROMATIC CONTENT

Aromatic content is the volume percent of aromatic molecules in the fuel. Since aromatic compounds are generally more difficult to burn cleanly, aromatic content is an important consideration in combustion quality. In addition, an increase in aromatic content acts to reduce the thermal stability of a fuel, thereby increasing the rate of deposition in fuel passages.

In general, aromatic content is inversely proportional to hydrogen content. This relationship is illustrated in Figure 5 (Reference 7). From Figure 5, the equation representing the relationship between fuel hydrogen content (H) and aromatic content (A) is

$$A = \frac{14.75 - H}{0.0583} \quad (6)$$

The sensitivity of aromatic content to hydrogen content can be expressed as follows:

$$\Delta A = \frac{\Delta H}{0.0583} \quad (7)$$

From the change in hydrogen content shown in Table 4, the increase in aromatic content for JP-4 and JP-8 can be found as

$$\text{JP-4 (Case 2): } \Delta A \approx +4.1\%$$

$$\text{JP-8 (Case 2): } \Delta A \approx +4.3\%$$

DISTILLATION CHARACTERISTICS

The fuel properties of viscosity, specific gravity, and volatility do not correlate well with final boiling point, freeze point, or smoke point. Changes in these properties can be inferred, however, from the change in distillation temperatures of the relaxed-specification fuels relative to the base fuels. Therefore, in order to estimate the effect of the proposed changes in JP-4 and JP-8 fuel specifications on viscosity, specific gravity, and volatility, several assumptions were made regarding the distillation characteristics of the relaxed-specification fuels.

The distillation curves for the base JP-4 and JP-8 fuels used in this study are shown in Figures 6 and 7. The curves drawn through the current specification data points in these figures represent an upper bound or limit to the range of individual distillation temperatures found for these fuels. Figure 8 illustrates this upper bound for a number of individual JP-4 fuels. As shown, while distillation temperatures for individual JP-4 fuels exhibit a wide variation over the distillation range, the current specification limits represent an upper bound for these temperatures. Similar curves were estimated for the relaxed-specification JP-4 and JP-8 fuels using the increase in final boiling point of 25°F, and are also shown in Figures 6 and 7. It should be emphasized that these proposed specification limit curves are not intended to represent the actual distillation characteristics of the relaxed-specification fuels, but rather an upper limit for the range of distillation temperatures which can be expected for these fuels, and are used to obtain a conservative estimate of the distillation temperatures for the relaxed-specification fuels.

The curves in Figures 6 and 7 were used to determine average distillation temperatures for the base and relaxed-specification fuels. The average distillation temperature, \bar{V} is defined as the arithmetic average of the 10%, 50%, and 90% distillation temperatures in °F. Table 5 presents estimated values of \bar{V} corresponding to the base fuels, the current specification limits, and the proposed specification limits for both JP-4 and JP-8. Values of \bar{V} for the base fuels and the proposed specification limits are subsequently used in correlations involving fuel viscosity and specific gravity. Since values of \bar{V} for the proposed specification limits represent an upper bound, changes in fuel viscosity and specific gravity found using these values represent conservative estimates.

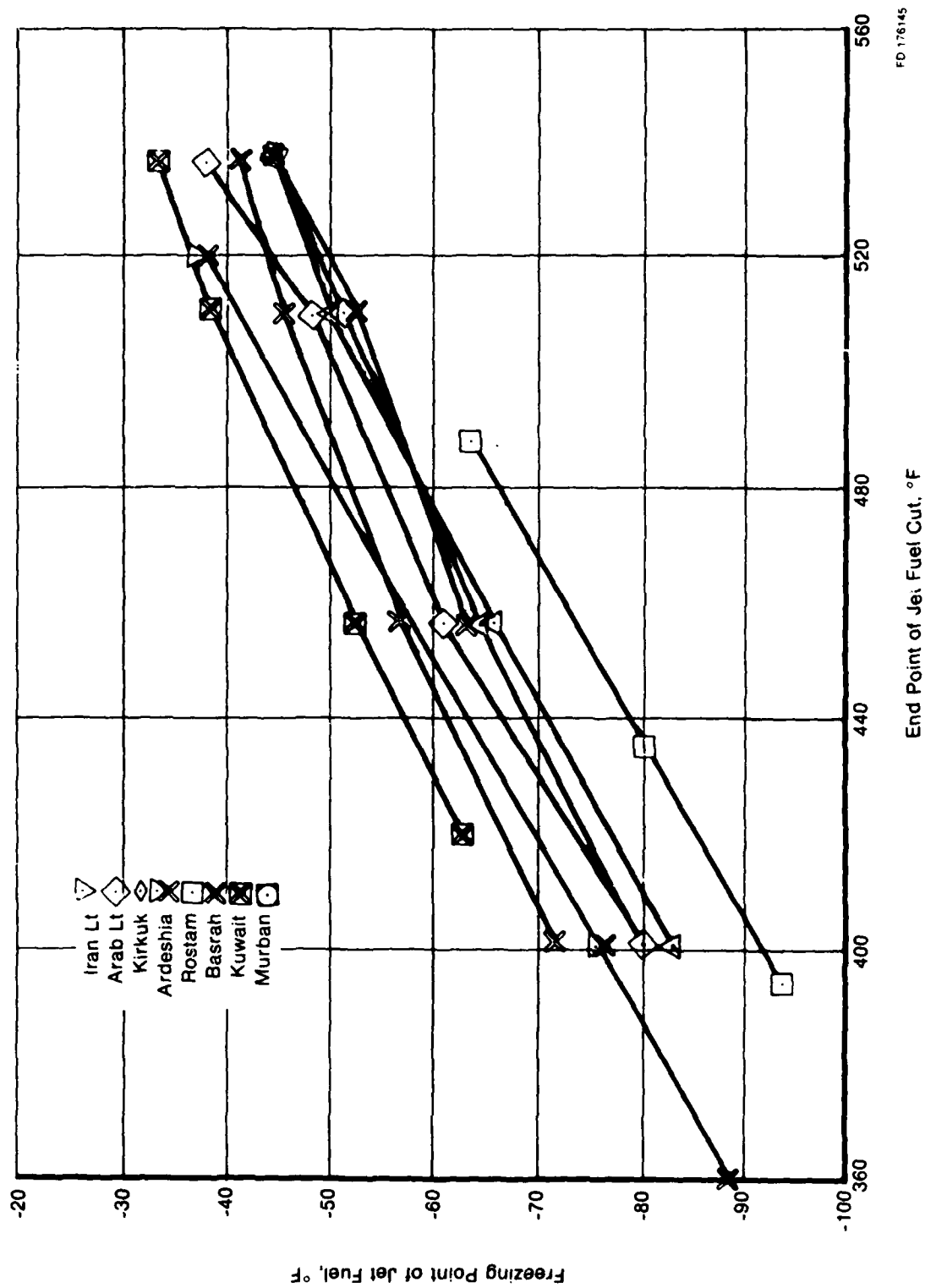
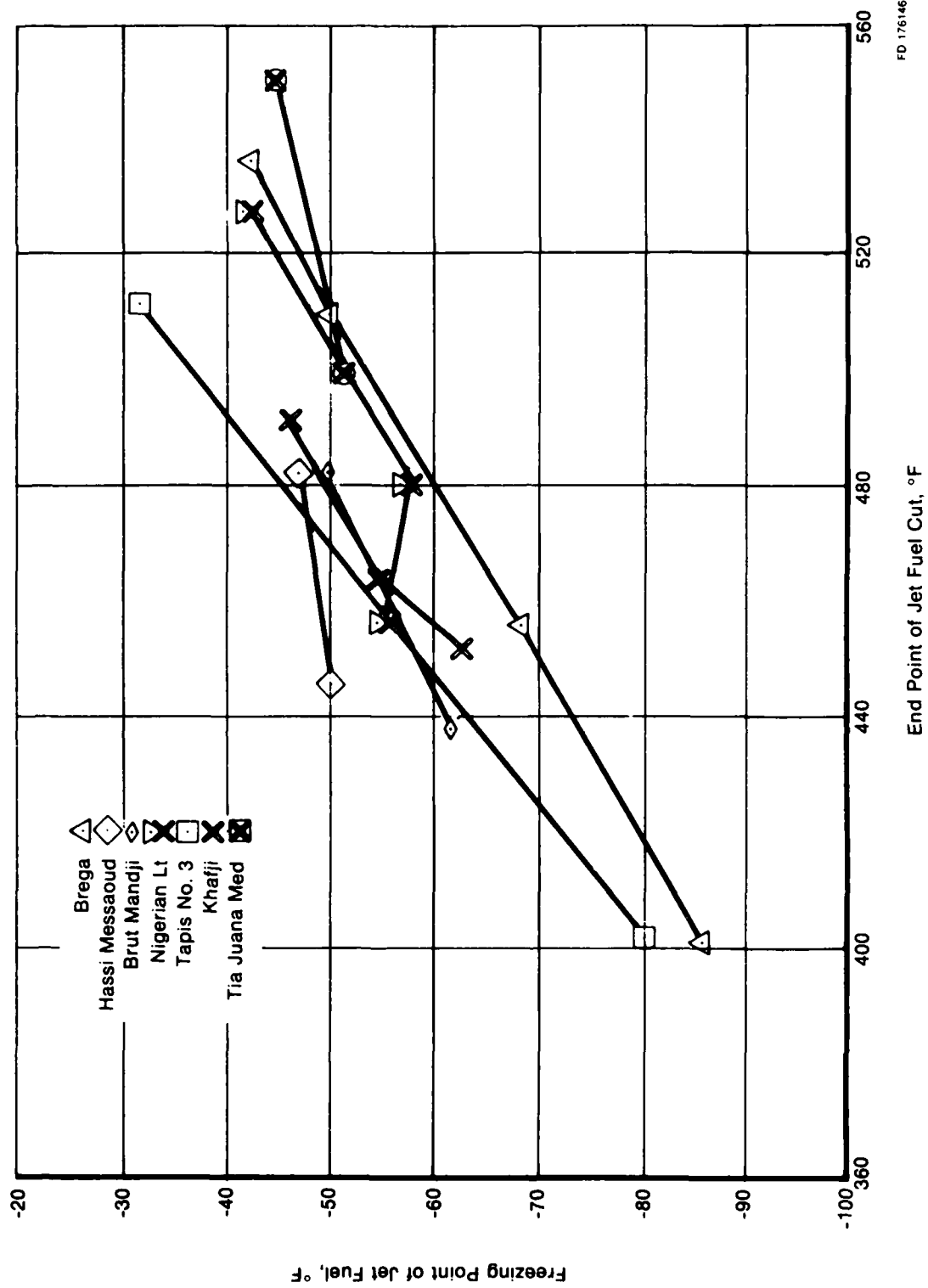


Figure 1. Jet Fuel Freezing Point vs Final Boiling Point for Mid-East Crudes



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Figure 2. Jet Fuel Freezing Point vs Final Boiling Point for African and Miscellaneous Crudes

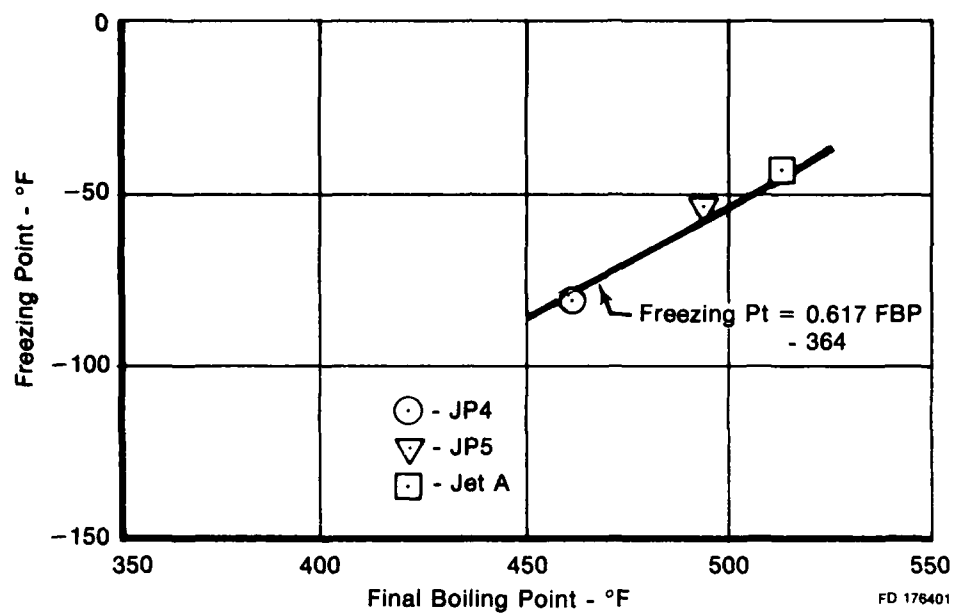
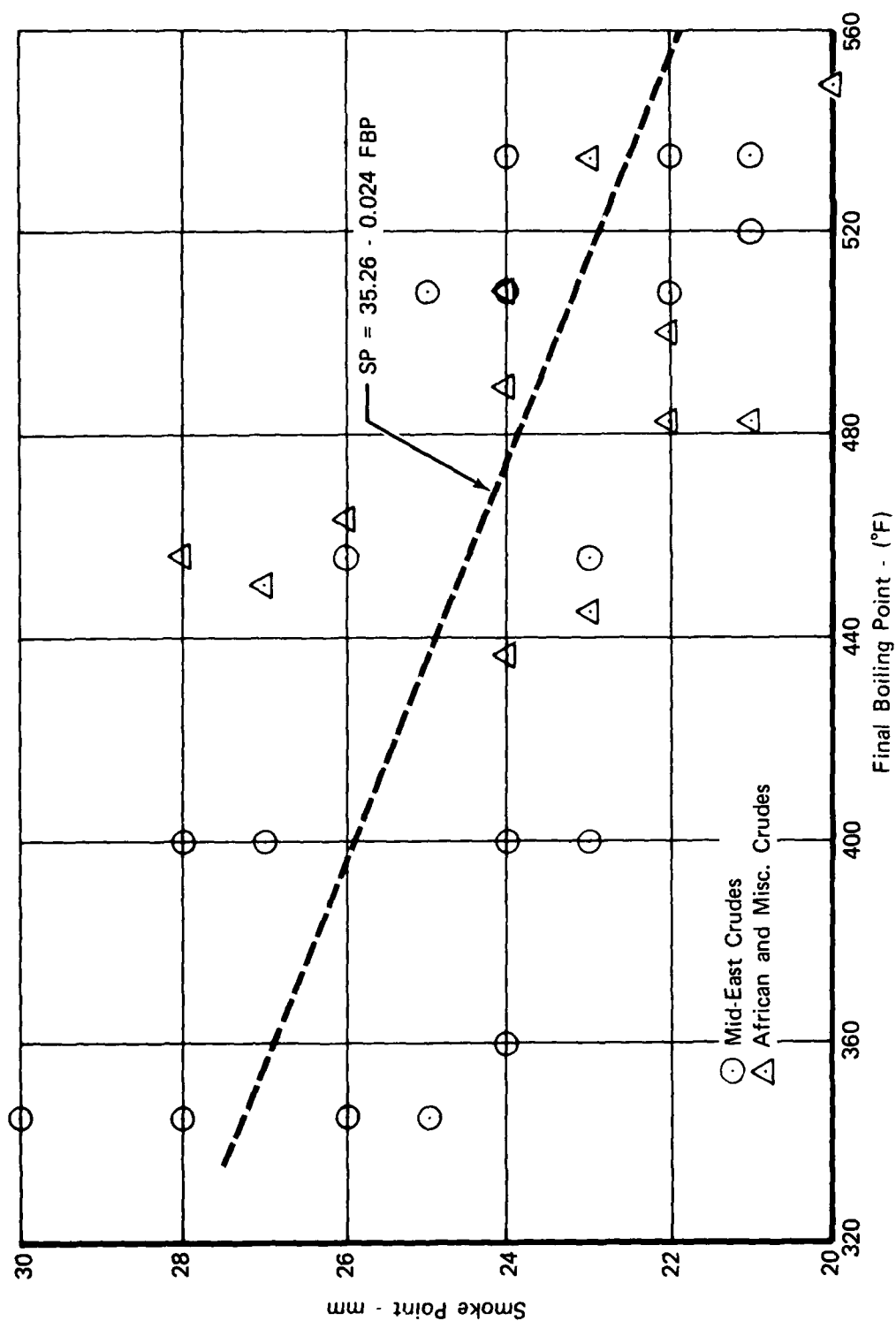


Figure 3. Correlation of Freeze Point With Final Boiling Point for Average Jet Fuels



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Figure 4. Correlation of Smoke Point With Final Boiling Point for Average Jet Fuels

TABLE 3. EFFECT OF CHANGES IN FINAL BOILING POINT ON FUEL SMOKE POINT

Fuel	Case	Base Fuel SP (mm)	ΔFBP °F	Implied ΔSP (mm)	Specified ΔSP (mm)
JP-4	1	28.1	+61	- 1.5	—
	2	28.1	+86	- 2.1	—
JP-8	1	25.0	+44	- 1.1	—
	2	25.0	+69	- 1.7	- 2.0

TABLE 4. EFFECT OF FUEL SPECIFICATION RELAXATIONS ON FUEL HYDROGEN CONTENT

Fuel	Case	ΔH (% by Weight)
JP-4	1	- 0.18
	2	- 0.24
JP-8	1	- 0.14
	2	- 0.25

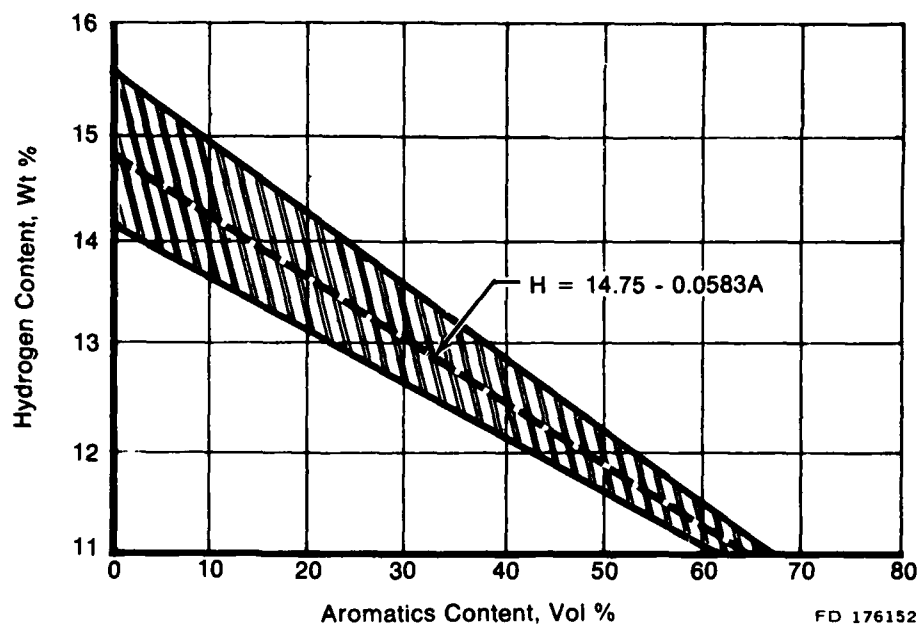
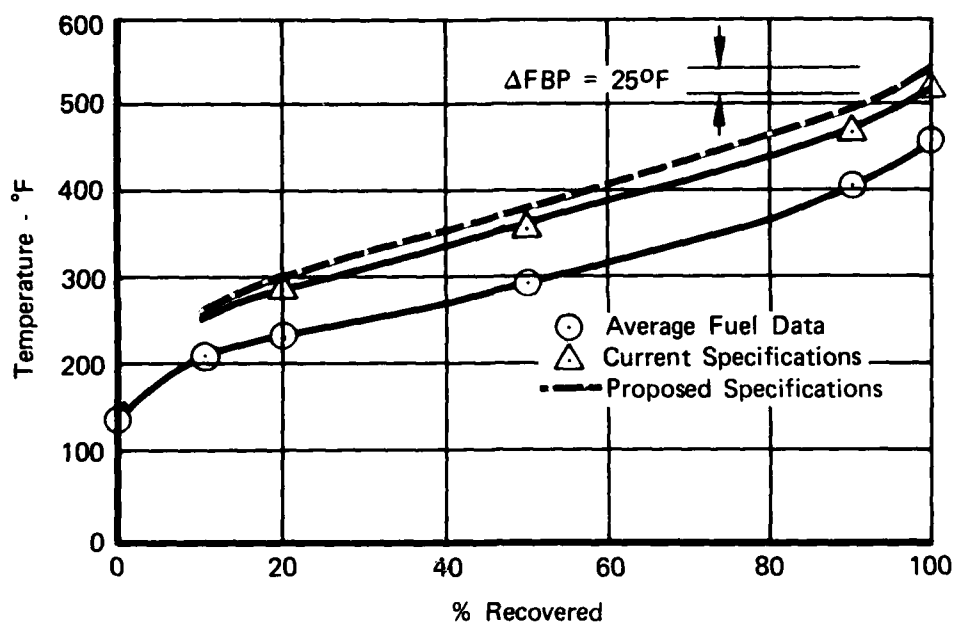
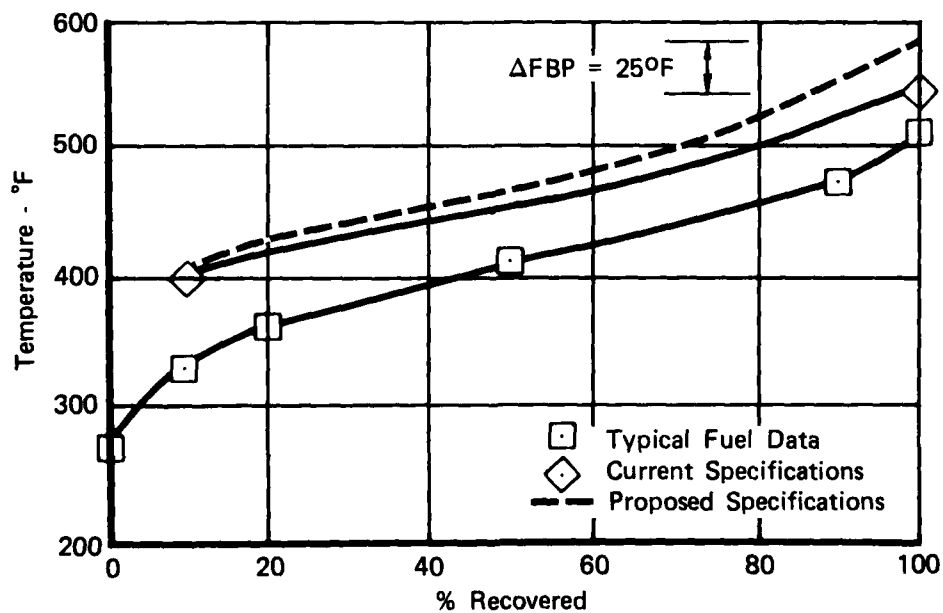


Figure 5. Variation of Hydrogen Content With Aromatics Content



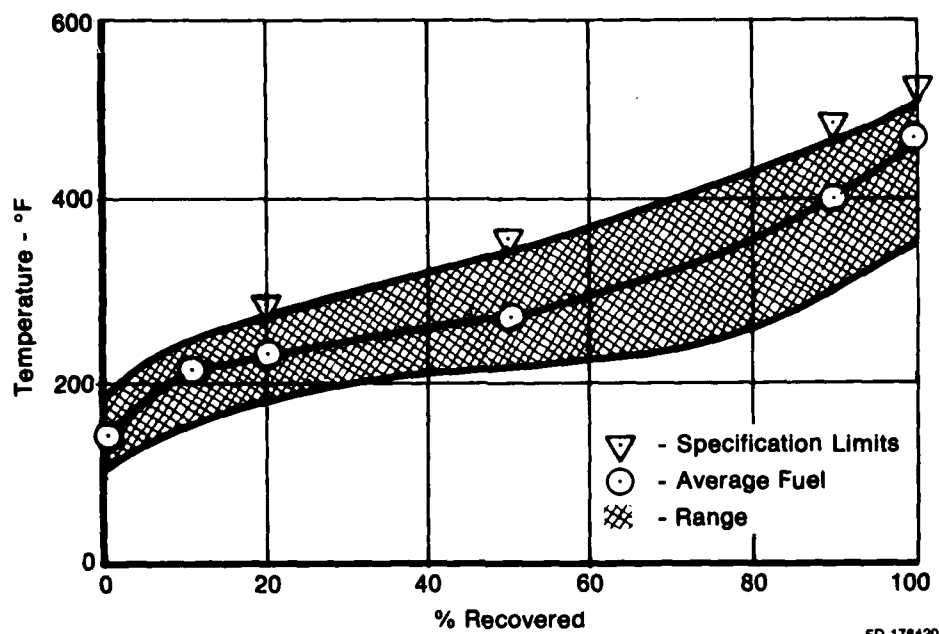
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Figure 6. Distillation Temperatures of the Base JP-4 Fuel and Specification Limits



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Figure 7. Distillation Temperatures of the Base JP-8 Fuel and Specification Limits



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Figure 8. Distillation Characteristics of JP-4 Fuels

TABLE 5. AVERAGE DISTILLATION POINTS

	JP-4	JP-8
Base	303	410
Current Specification Limits	378	467
Proposed Specification Limits	393	480

VISCOSITY

Viscosity is a measure of the fluidity of fuel and an important consideration in pumping and measuring devices. It is particularly important in the performance of the fuel nozzle where it impacts the atomization quality. The kinematic viscosity is commonly used to characterize this property because it combines the effect of density with absolute viscosity.

A correlation of kinematic viscosity, ν , with average distillation temperature, \bar{V} , is shown in Figure 9. Data points shown in Figure 9 represent average values of ν and \bar{V} for Jet A-1, JP-5, JP-4, and No. 2 fuel oil at temperatures of 0°F, 100°F and 200°F (from References 6, 8 and 9). Equations representing the dependence of ν on \bar{V} were found from regression analyses to be

$$\nu(\text{at } 0^\circ\text{F}) = \exp [0.0090 (\bar{V}) - 1.82] \quad (8)$$

$$\nu(\text{at } 100^\circ\text{F}) = \exp [0.0053 (\bar{V}) - 1.58] \quad (9)$$

$$\nu(\text{at } 200^\circ\text{F}) = \exp [0.0031 (\bar{V}) - 1.42] \quad (10)$$

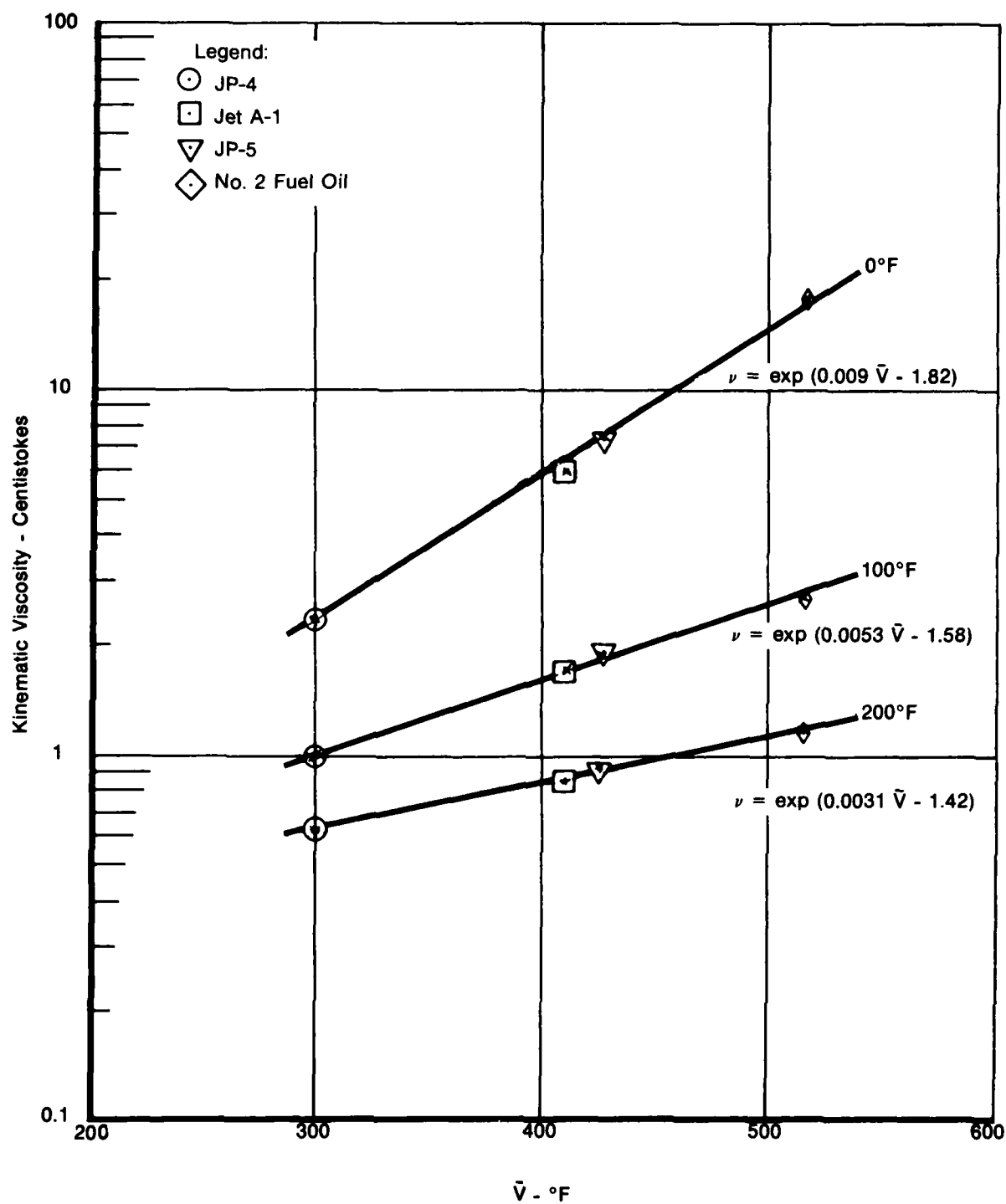
Equations 6 through 8 can be rearranged to find the ratio of a relaxed specification fuel viscosity to a base fuel viscosity as follows:

$$\nu/\nu_b (\text{at } 0^\circ\text{F}) = \exp [0.0090 (\Delta\bar{V})] \quad (11)$$

$$\nu/\nu_b (\text{at } 100^\circ\text{F}) = \exp [0.0053 (\Delta\bar{V})] \quad (12)$$

$$\nu/\nu_b (\text{at } 200^\circ\text{F}) = \exp [0.0031 (\Delta\bar{V})] \quad (13)$$

Using Equations 11 through 13, the ratio ν/ν_b was calculated from the estimated changes in \bar{V} (from Table 5) at Case 2 conditions. The results are presented in Table 6 for both JP-4 and JP-8 fuels. As is shown, fuel temperature has a significant effect on the increase in viscosity due to a relaxation of fuel specifications. Equations of the general form of Equations 11 through 13 were developed for several additional fuel temperatures ranging from -30 to 200°F. The resulting ν/ν_b ratios are shown plotted against fuel temperature in Figure 10. In addition, viscosity data on JP-4 and JP-5 fuels (Reference 8) were used to determine the viscosity ratio of JP-5 relative to JP-4 at several fuel temperatures. As is shown, the increase in the Case 2 JP-4 fuel viscosity relative to the base JP-4 fuel ranges from approximately 270% at -30°F to approximately 30% at 200°F, while the increase in Case 2 JP-8 viscosity relative to the base JP-8 fuel decreases from approximately 115 to 24% over the same range of fuel temperatures. Figure 10 indicates that as temperature is increased, the impact of a relaxed specification fuel on fuel nozzle atomization quality decreases. Therefore, increases in fuel viscosity should have the most significant effect on cold-day start-up characteristics of an engine, when fuel temperatures are relatively low.

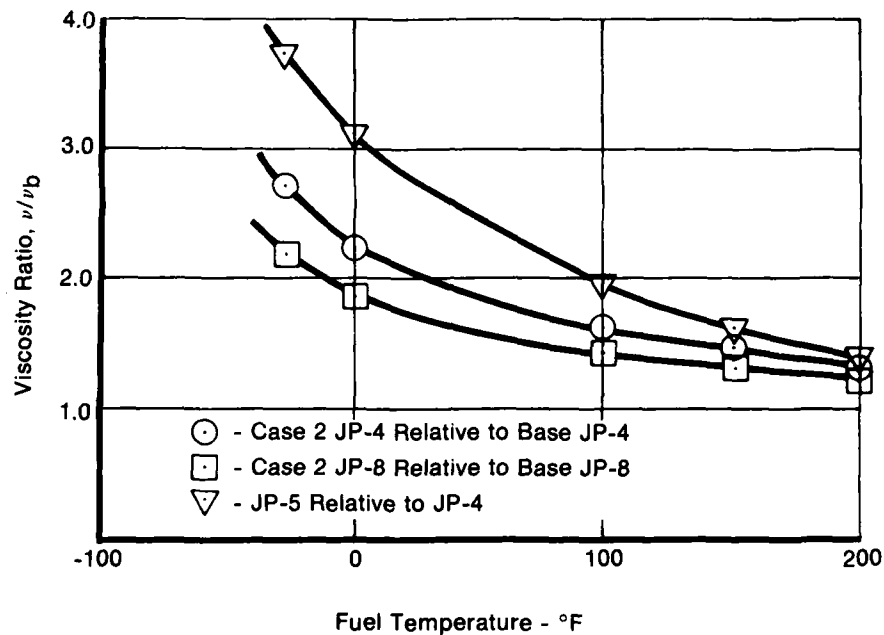


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Figure 9. Correlation of Kinematic Viscosity With Average Distillation Temperature

TABLE 6. EFFECT OF PROPOSED FUEL SPECIFICATION RELAXATIONS ON KINEMATIC VISCOSITY

Fuel	Case	ν/ν_b		
		at 0°F	at 100°F	at 200°F
JP-4	2	2.25	1.61	1.32
JP-8	2	1.88	1.45	1.24



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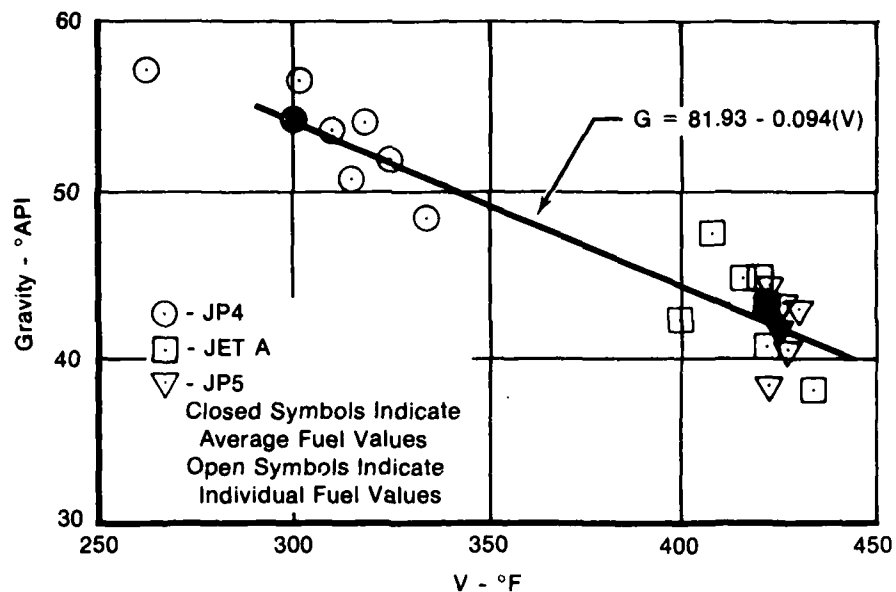
Figure 10. Changes in Viscosity Ratio With Changes in Fuel Temperature

SPECIFIC GRAVITY

Specific gravity reflects the weight per unit volume of the fuel, and combined with the heating value of the fuel, affects a given aircraft's range and endurance. Variations in specific gravity must be accounted for in the engine control to ensure the correct fuel flow to the combustor. For example, changing from JP-5 to JP-4 fuel without adjustment of the fuel control can result in excessively high temperatures in the combustor and turbine. In addition, fuel density may affect fuel nozzle atomization quality and penetration of the fuel spray.

A correlation between API gravity and average distillation temperature was developed using average JP-4, JP-5, and Jet A data (Reference 6), and is shown in Figure 11. Data from an analysis of individual fuels are also included in Figure 11. The functional relationship between API gravity, G , and average distillation temperature, \bar{V} , indicated in Figure 8 is:

$$G = 81.93 - 0.094 \bar{V} \quad (14)$$



FD 176150

Figure 11. Correlation of API Gravity With Average Distillation Temperature

Using this equation, and the calculated values of \bar{V} (Table 5), the ratio of Case 2 API gravity to base API gravity was found for both JP-4 and JP-8 and is shown in Table 7.

Specific gravity, SG, is related to API gravity by the following equation:

$$SG = \frac{141.5}{G + 131.5} \quad (15)$$

Using Equation 15, the ratio of Case 2 to base fuel specific gravity was found for both fuels, and these values are also given in Table 7. As is indicated, the change in fuel specifications has a relatively small effect on specific gravity, with estimated (conservative) increases of 5 and 4% for JP-4 and JP-8, respectively.

TABLE 7. EFFECT OF PROPOSED FUEL SPECIFICATION RELAXATIONS ON API GRAVITY AND SPECIFIC GRAVITY

Fuel	Case	G/G_b	SG/SG_b
JP-4	2	0.84	1.05
JP-8	2	0.85	1.04

VOLATILITY

Increases in the final boiling point of a fuel act to decrease fuel volatility. A decrease in fuel volatility acts to lower the rate of fuel vaporization in a combustor, thereby aggravating altitude ignition and lean-blowout problems and decreasing combustion efficiency.

There are a number of indicators of fuel volatility, including initial boiling point (IBP), the 10, 20, and 25% distillation temperatures, and Reid vapor pressure (RVP). No attempt was made in this study to determine the effect of the proposed fuel specification relaxations on RVP or IBP.

However, the estimated distillation characteristics shown in Figures 6 and 7 may be used to obtain the distillation temperatures mentioned above. These temperatures are given in Table 8 corresponding to the base JP-4 and JP-8 fuels and the relaxed specification JP-4 and JP-8 fuels.

The distillation temperatures shown in Table 8 indicate that the proposed specifications will have a significant effect on fuel volatility for both fuels. However, as mentioned previously, the estimated proposed specification limit curves shown in Figures 1 and 2 represent an upper bound for relaxed-specification fuel distillation temperatures. Therefore, actual changes in fuel volatility resulting from a +25°F increase in FBP should be much smaller than those indicated in Table 7.

TABLE 8. EFFECT OF PROPOSED FUEL SPECIFICATION RELAXATIONS ON FUEL VOLATILITY (AS INDICATED BY THE 10%, 20%, AND 25% DISTILLATION TEMPERATURES)

	Distillation Temperature (°F)		
	10%	20%	25%
Base JP-4 Fuel	211	233	241
Relaxed Specification JP-4 Fuel	260	300	315
Base JP-8 Fuel	340	364	370
Relaxed Specification JP-8 Fuel	400	415	425

HEAT OF COMBUSTION

Heat of combustion is a measure of the potential heat release in combustion of a fuel, and thus is fundamental to the operation of a gas turbine engine. The caloric value of a fuel along with the specific gravity are important in determining aircraft range and endurance. Variations in heat of combustion must be compensated for in the engine fuel control. Fortunately, there is little variation in heat of combustion for most current jet fuels. For example, the heat of combustion of Jet A fuel is approximately 150 B/lbm less than that of JP-4. Therefore, changes in fuel caloric value due to the proposed fuel specification relaxations were assumed to be negligible in this study.

THERMAL STABILITY

Fuel in many high-performance aircraft is subject to high thermal stresses resulting from its use as a heat sink. Thermal stability is a measure of a fuel's ability to withstand this stress without degradation. This ability is extremely important in the operation of the key fuel system components including the fuel control and the fuel nozzles. The result of inadequate thermal stability is the deposition of fuel degradation products on the internal surface of fuel system components. The thermal stability is quantified in the JFTOT test procedure by either a pressure drop across a filter and/or a color-code rating of deposits in the test section.

Based on average fuel data (Reference 6), the filter pressure drop is approximately 5.1 mm Hg for JP-4 fuel, and approximately 10.2 mm Hg for JP-8 fuel (using Jet A data). Since these values are well below the current specification limit of 25 mm Hg (maximum), the relaxed specification fuels being considered in this study are expected to pass the JFTOT test.

Another measure of fuel thermal stability is the break point temperature (TBP). The TBP is defined as the maximum temperature at which a fuel will pass the JFTOT test, or the minimum temperature at which a fuel will fail the JFTOT test. Lohmann (Reference 10) has developed the following TBP correlation based on coal- and shale-derived fuels:

$$\text{TBP (°F)} = 255 + 259 (S) - 0.024 (N) - 0.00418 (Na) - 0.0148 (O) - 0.082 (A) - 0.067 \quad (16)$$

where

- S = Sulfur Content (Percent by Weight)
- N = Nitrogen Content (Percent by Weight)
- Na = Naphthalene Content (Percent by Volume)
- Ol = Olefin Content (Percent by Volume)
- A = Aromatic Content (Percent by Volume)

No attempt was made in this study to estimate the increase in these fuel constituents (other than aromatics) resulting from the proposed changes in fuel properties. However, using the correlation of Equation 16, and the estimated increase in aromatics found previously, a decrease in TBP of approximately 1% is predicted for the relaxed specification JP-4 and JP-8 fuels. Therefore, the decrease in thermal stability of JP-4 and JP-8 fuels resulting from the proposed changes in fuel properties is expected to be negligible.

SECTION III

EFFECT OF FUEL PROPERTIES ON THE OPERATIONAL CHARACTERISTICS OF GAS TURBINE ENGINES

This section presents the findings of an extensive literature survey conducted to review and discuss the effects of broadened-specification fuels on the performance and durability of gas turbine engines. Both chemical and physical fuel properties are considered, and their potential impact on operation of the J57, TF30 and F100 engines is discussed. These discussions are qualitative in nature, and deal primarily with relaxed-specification fuels in general. When possible, however, the effect of the proposed relaxations of JP-4 and JP-8 fuel specifications on engine performance are estimated using available data in the literature.

EFFECT OF FUEL PROPERTIES ON COMBUSTOR PERFORMANCE

Ignition Characteristics

The mechanism of ignition of a combustible mixture has been the subject of numerous experimental and analytical investigations which have characterized the process in terms of combustor design parameters as well as the chemical aspects of the combustible mixture. In the gas turbine combustor, ignition of the flowing mixture of air and fuel is effected by the passage of an electrical discharge through the mixture. The passage of this spark creates a cylindrical volume of hot gas between the electrodes. During the spark duration, the flow extends this volume of hot gases in the downstream direction. If the rate of heat release due to chemical reaction within this volume exceeds the rate of heat loss at the kernel surface due to turbulent diffusion, a successful ignition is achieved. If not, the temperature within the kernel will drop causing cessation of further reaction. Thus, the critical factor is the size to which this kernel has grown at the time the temperature of the gases within the kernel have fallen to the normal flame temperature of the mixture. Thus, the criterion for survival of the spark kernel is that the minimum dimension of the kernel should always exceed the quenching distance, d_q . The energy required to heat a volume of gas, whose minimum dimension is equal to the quenching distance, to the adiabatic flame temperature is called the minimum ignition energy (MIE).

Simple theory predicts that the MIE varies with the cube of the quenching distance. The MIE and d_q are both dependent on the operational characteristics of the combustor and on the chemical composition of the fuel. The variation in the MIE with quenching distance for stoichiometric mixtures of methane and propane at various pressures, flow velocities, and oxidant partial pressures at low turbulence levels, is shown in Figure 12 (Reference 11). The combustor operational characteristics which are known to impact the minimum ignition energy are mainstream flow velocity, pressure, temperature, turbulence intensity, and turbulence scale. The effect of velocity is both beneficial and detrimental to ignition. The velocity displaces the spark downstream thereby reducing loss of heat and reactive species to the electrodes. It also tends to shield the spark by increasing the length of the kernel and hence, increases the amount of energy released during the discharge spark. However, the flow velocity is detrimental in that it increases the convective heat loss from the surface of the kernel during the spark discharge and subsequent to the discharge, it increases the heat loss due to turbulent diffusion. The net effect of the velocity is a slight increase in MIE, as shown in Figure 13 for methane/oxygen and propane/oxygen mixtures (Reference 11).

The minimum ignition energy generally increases with a decrease in pressure as indicated by the relation $(MIE) = (MIE)_0 (P/P_0)^{-n}$ (Reference 12) where the subscript 0 refers to standard atmospheric pressure. The exact value of the exponent is dependent on hydrocarbon type. A logarithmic plot of MIE vs pressure is shown in Figure 14 for various stoichiometric methane/air mixtures. In this case, the pressure exponent is 0.95 while for heavier hydrocarbon-type fuels, it is closer to 2 (Reference 13).

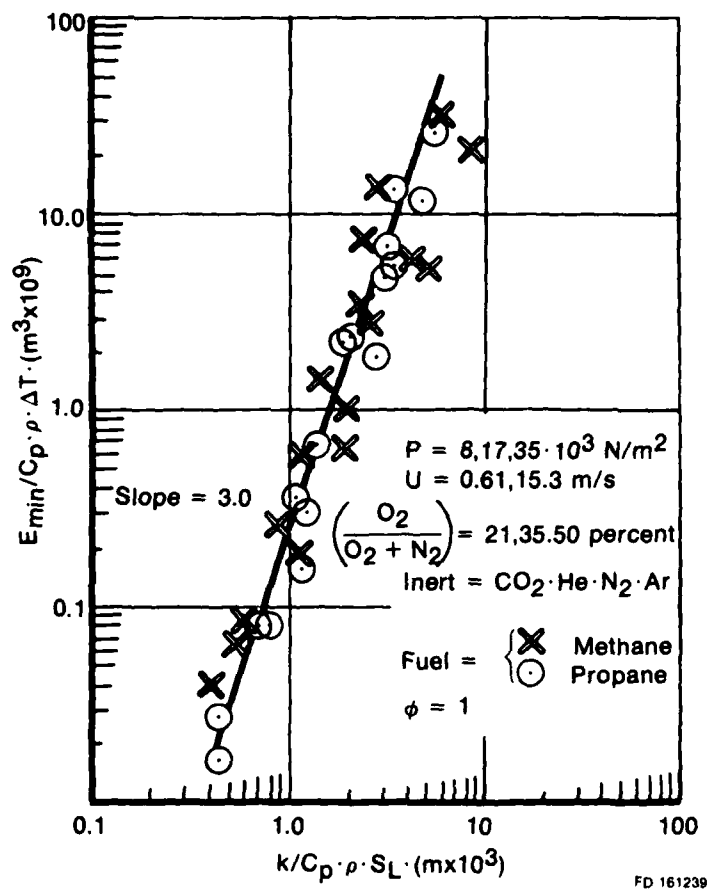


Figure 12. Minimum Ignition Energy vs Quenching Distance Under Low Turbulence Conditions

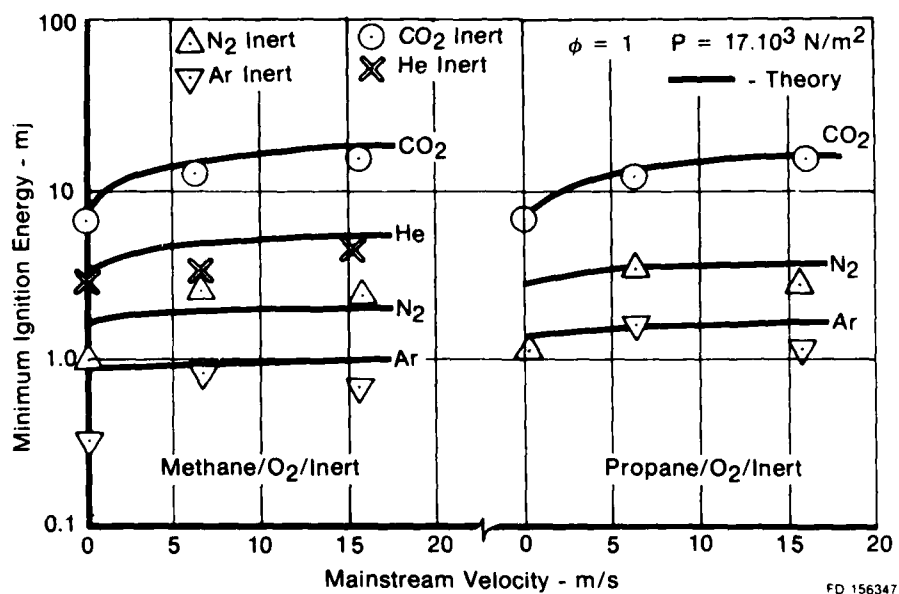


Figure 13. Influence of Mainstream Velocity on Minimum Ignition Energy

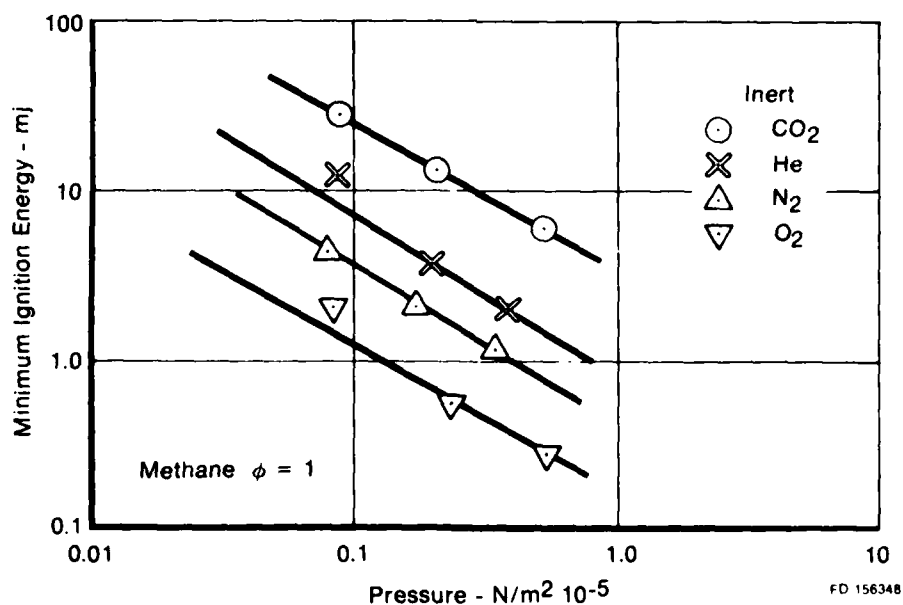


Figure 14. Logarithmic Plot of Minimum Ignition Energy vs Pressure

The effect of turbulence is both to increase the loss of heat from the spark kernel to the surrounding cold mixture and to increase the heat generated in combustion. It does this by wrinkling the flame front and thereby increasing its surface area. The net effect of the turbulence is always to increase the ignition energy. The effect of turbulence intensity is shown parametrically in the curves of MIE vs equivalence ratio for propane in Figure 15. The effect of turbulence scale depends on the level of turbulence intensity. At low turbulence level, the MIE tends to decrease with increasing scale. At high turbulence levels, MIE increases with turbulence scale. This is shown in Figure 16 for stoichiometric methane/oxygen mixtures.

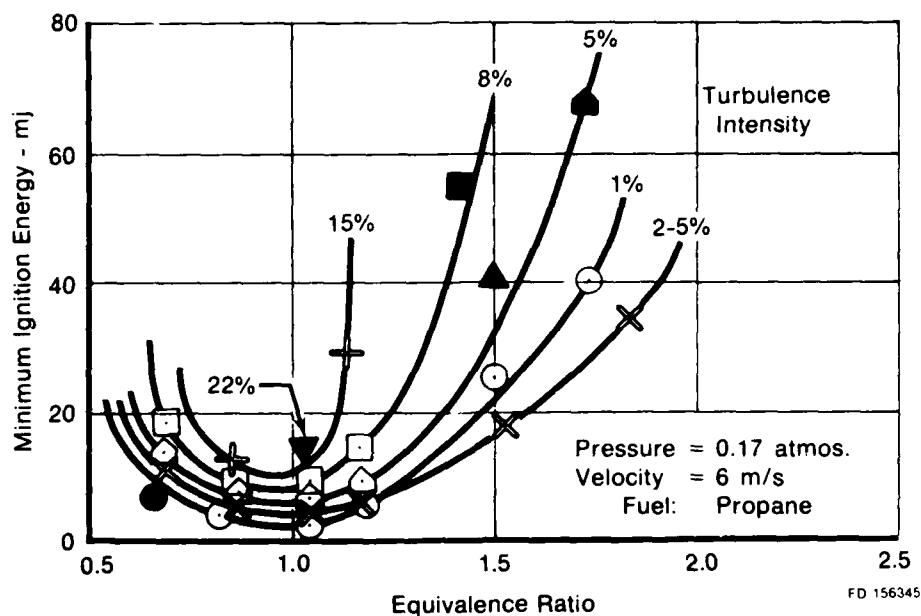


Figure 15. Graphic Plots Illustrating the Influence of Turbulence Intensity on Minimum Ignition Energy for Propane/Air Mixtures

In addition to the physical variables presented above, the ignition energy is also dependent on the chemical and physical nature of the combustible mixture. The most obvious requirement is that the stoichiometry of the mixture must be within the flammability limits of the fuel. However, even within this range, the MIE is dependent on the fuel/air ratio. The minimum ignition energy for simple paraffinic hydrocarbons occurs near equal stoichiometric equivalents of air and fuel or unit equivalence ratio. The MIE tends to shift to higher equivalence ratios (fuel rich) with increasing molecular complexity of the fuel. This effect is shown in Figure 17 (Reference 12). The effect of molecular structure on MIE and d_q is also shown in Table 9. The effect of the degree of unsaturation is shown in the two-carbon atom series, ethane (paraffinic), ethylene (olefinic), and acetylene (alkyne). For simple aliphatic hydrocarbons, the MIE and d_q decrease with an increase in the degree of unsaturation. The next three compounds form a six-carbon aliphatic compound, n-hexane has a higher MIE than the corresponding six-carbon cyclic compound, cyclohexane. In this example, there is no effect on MIE of aromatization in that cyclohexane has the same MIE as benzene which is an aromatic compound.

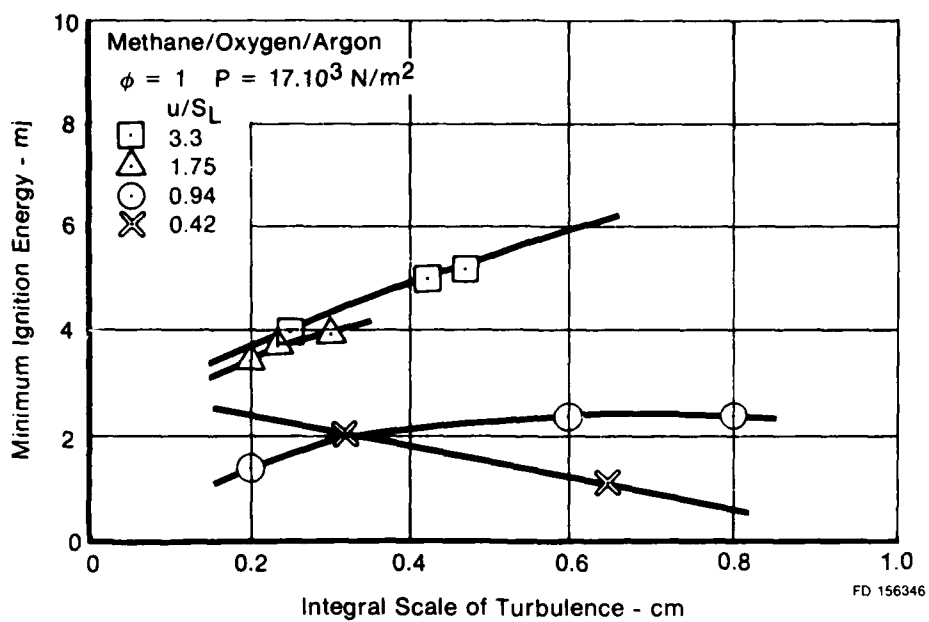


Figure 16. Influence of Turbulence Scale on Minimum Ignition Energy

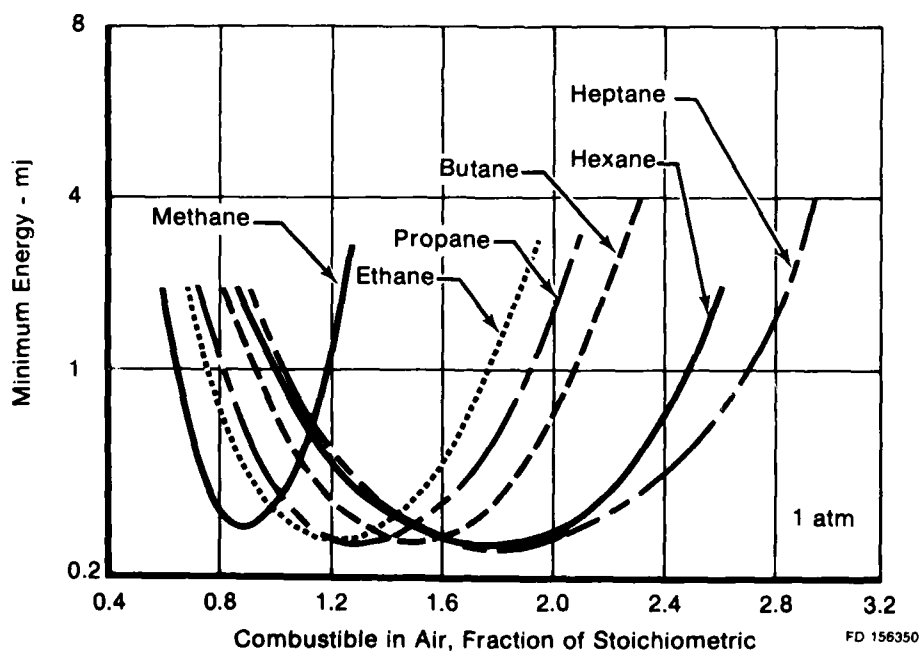


Figure 17. Minimum Ignition Energy for Various Hydrocarbons

TABLE 9. MINIMUM IGNITION ENERGIES (MIE) AND QUENCHING DISTANCES (d_q) OF SELECTED HYDROCARBONS IN AIR AT ATMOSPHERIC PRESSURE

	MIE (mj)	d_q (in.)
Ethane	0.25	0.09
Ethylene	0.07	0.05
Acetylene	0.02	0.025
n-Hexane	0.29	0.06
Cyclohexane	0.22	0.07
Benzene	0.22	0.07

The effect of molecular structure of the fuel on ignitability is extremely complex and readily becomes obscured by physical properties of the fuel when more complicated examples are considered. Korber (Reference 14) examined the ignitability of some paraffinic, aromatic, and naphthalenic compounds and concluded that the ignitability of cyclic compounds is less than that of paraffinic compounds. Within a series of double-ringed molecules (decalin, tetralin, and methyl-naphthalene), the ignitability decreased with increasing extent of unsaturation. However, in this test the results were dependent upon the vaporization of these fuels, each of whose volatility differs significantly, and thus, it is not clear that the observed effect was indeed due to the chemical nature of the fuel and not simply due to the physical properties which are a result of the molecular structure.

The primary impact of the fuel is thus through its physical properties rather than the chemical nature of the fuel molecules. The ignition mechanism described earlier assumes the passage of a spark through a combustible gaseous fuel-air mixture. In aircraft gas turbine combustors, which consume liquid fuels, it is the preparation of this mixture which limits combustor ignition performance. In all combustor designs, the atomization of the liquid fuel is achieved through the use of a fuel nozzle. However, the mechanism of this atomization may vary from one design to another. The J57 and TF30 both utilize pressure-atomizing fuel nozzles, where the energy required to break up the liquid fuel into droplets comes primarily from the mechanical fuel pressure. The F100 combustor, on the other hand, utilizes an airblast nozzle which utilizes the air pressure drop across the burner front bulkhead to atomize the fuel.

The performance of a pressure-atomizing fuel nozzle is determined by the nature of the fuel and the particular design of the nozzle, and is normally characterized by the resultant fuel droplet size and penetration. The most significant physical properties of the fuel which enter into the atomization process are fuel density, viscosity, and surface tension. The effect of these properties on fuel droplet size, characterized in terms of Sauter Mean Diameter (SMD), can be estimated using the following equation (Reference 15):

$$\text{SMD} = K \rho_f^{0.25} \nu_f^{0.20} \sigma_f^{0.60} \Delta P^{-0.40} \quad (17)$$

where ρ_f , ν_f , and σ_f are the fuel density, viscosity, and surface tension, respectively. K is a constant dependent upon the particular fuel nozzle design. ΔP is the nozzle driving pressure.

The dependence of SMD on fuel viscosity is shown in Figure 18 for a pressure-atomizing nozzle. As indicated, an increase in viscosity from 2.2 to 18.5 centistokes yields an increase in SMD of approximately 50%.

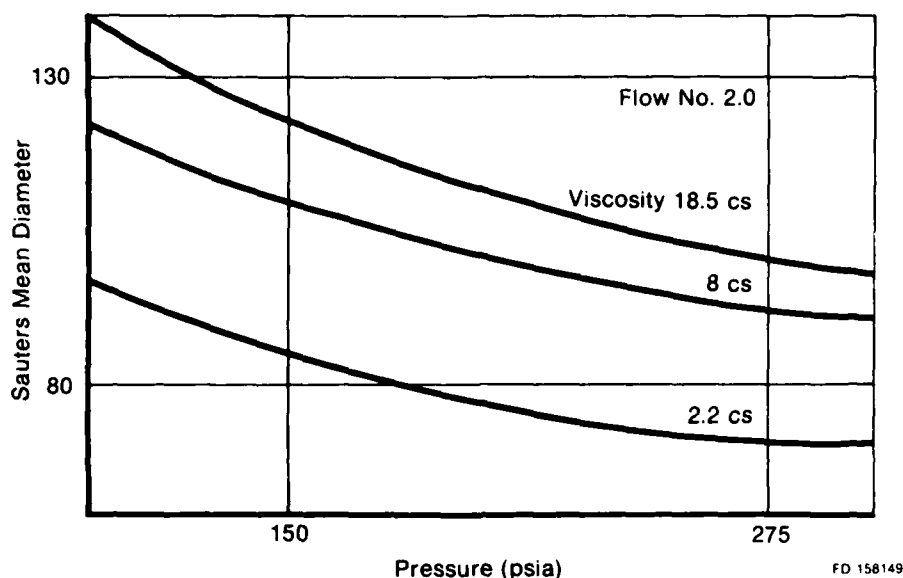


Figure 18. Effect of Viscosity on Droplet Size

The effect of viscosity on the nozzle performance depends upon the driving pressure. This variation in nozzle performance is shown in Figure 19 (Reference 16). The fuel nozzle flow number (F_N) is defined to be the volumetric flow divided by the square root of the driving pressure (P), $F_N = Q/\sqrt{P}$, and as such is representative of the discharge coefficient. Figure 19 shows this variation in flow number with viscosity for two different injection pressures. The sensitivity of flow number to viscosity is obviously much greater at low pressure than at high pressure. This effect is particularly significant for the ignition process of a gas turbine combustor such as the J57 and TF30. Ground starts and airstarts for these combustors are normally achieved under conditions where the fuel flow determined by the engine fuel control device is at a minimum. This means, consequently, that the fuel pressure drop across the nozzle, which effects the atomization, is also at a minimum. Thus, small viscosity changes, which would be insignificant at higher engine thrust conditions, may result in serious deficits in nozzle performance under startup conditions.

Fuel density and surface tension have much less of an impact on fuel nozzle performance. The above empirical correlation for SMD indicates only a 6% increase in size for a 10% increase in surface tension. However, the surface tension of hydrocarbon fuels derived from petroleum sources usually do not differ even by 10%. The effect of density on nozzle performance is a slight decrease in spray cone angle and a slight increase in spray penetration with increasing density. These effects are usually considered negligible.

The fuel nozzle in the F100 engine combustor represents a significantly different approach to atomization of the fuel than the pressure-atomizing nozzles in the J57 and TF30 combustors. The F100 combustor fuel nozzles are of the airblast type in which atomization is achieved primarily due to the airflow across the fuel nozzle. Consequently, the performance of the nozzle is dependent both on fuel properties and airflow conditions. An empirical correlation for droplet size resulting from an airblast nozzle is presented in terms of fuel density (ρ_f), surface tension (σ_f), viscosity (ν_f), fuel mass flowrate (W_f), air mass flowrate (W_a), air density (ρ_a), and the pressure drop across the nozzle (ΔP_n) as (Reference 17):

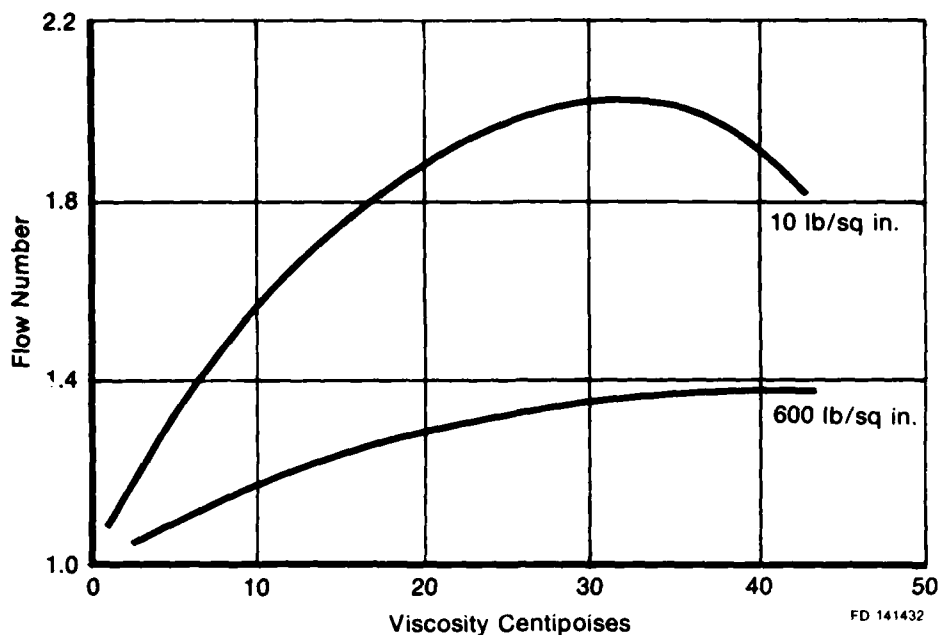


Figure 19. Variation of Flow Number With Fuel Viscosity

$$\text{SMD} = K \Delta P_n^{-0.50} \rho_n^{-0.875} (1 + W_f/W_n) \sigma_f^{0.375} \nu_f^{0.06} \rho_f^{0.25} \quad (18)$$

where K is a constant for a particular nozzle. The operative term in the correlation is $(1 + w_f/w_n)$. This parameter varies with combustor fuel-air ratio and hence, with engine operating condition. Assuming a datum for w_f/w_n of unity, the relative effect of fuel-air ratio excursions on droplet size is shown in Figure 20 (Reference 18). The design operating nozzle fuel-air ratio is determined as a compromise between requirements for nozzle performance and combustion characteristics in the primary zone of the combustor. The F100 combustor operates at SLTO with approximately a w_n/w_f of unity. At lower power conditions both airflow and fuel flow are reduced, but not in equal proportions, so that at Idle the air-fuel ratio is approximately 1.5. This range of variation falls in the relatively flat portion of the curve in Figure 20, thereby minimizing changes in fuel droplet size with engine power setting. This illustrates one of the advantages of airblast-type fuel nozzles.

The dependence of droplet size for an airblast nozzle on fuel viscosity is predicted by the above empirical equation to exhibit a 0.06 exponent variation. This apparent insensitivity to fuel viscosity is another advantage of airblast nozzles. However, the experience at P&WA in the use of airblast nozzles indicates a somewhat greater dependence on viscosity than the 0.06 power. Our experience with the F100 fuel nozzle indicates that droplet size is proportional to the 0.13 power of viscosity. Even in this case, the airblast nozzle still exhibits less sensitivity to changes in fuel viscosity than pressure-atomizing nozzles.

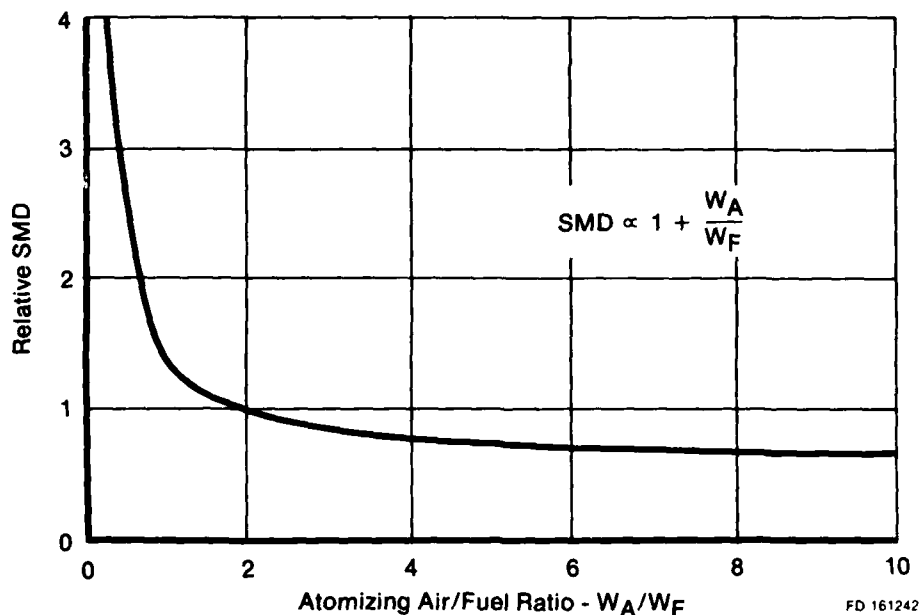


Figure 20. Effect of Air/Fuel Ratio Variations on Relative Droplet Size

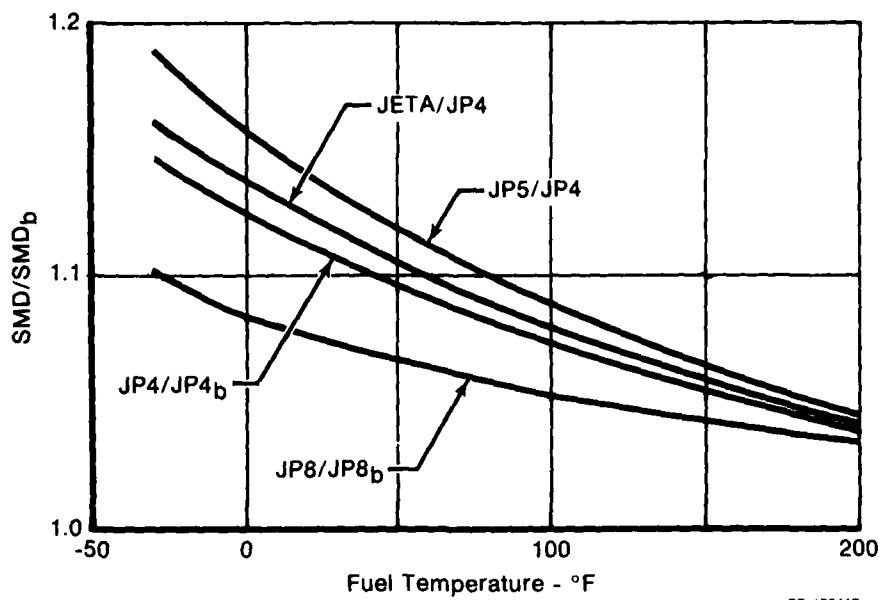
Figures 21 and 22 illustrate the sensitivity of both the pressure-atomizing and airblast nozzles to changes in fuel viscosity. These curves were derived through the use of Equations 17 and 18 to calculate the ratio of SMD for a given fuel relative to a base fuel. Thus, for pressure-atomizing nozzles,

$$\frac{SMD}{SMD_b} = \left(\frac{\rho}{\rho_b} \right)^{0.25} \left(\frac{\sigma}{\sigma_b} \right)^{0.80} \left(\frac{\nu}{\nu_b} \right)^{0.20} \quad (19)$$

and for the airblast nozzles,

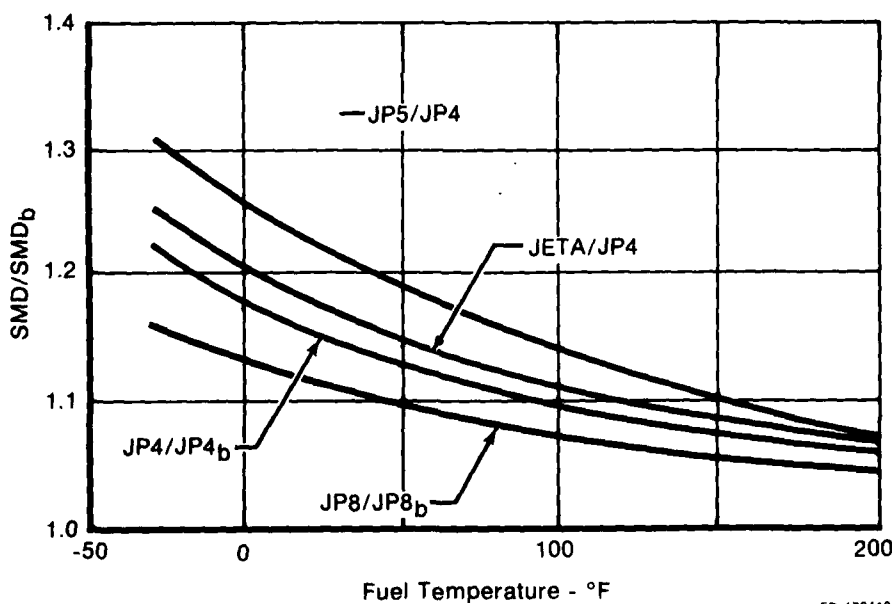
$$\frac{SMD}{SMD_b} = \left(\frac{\rho}{\rho_b} \right)^{0.25} \left(\frac{\sigma}{\sigma_b} \right)^{0.375} \left(\frac{\nu}{\nu_b} \right)^{0.18} \quad (20)$$

where ρ/ρ_b , σ/σ_b , and ν/ν_b are the density, surface tension, and viscosity ratios of a given fuel relative to the base fuel. All other factors are assumed to be equal. Equations 19 and 20 assumed fuel density and viscosity ratios to be unity since the changes are small. The SMD ratio was found for the relaxed specification JP-4 and JP-8 fuels relative to their respective base fuels using the estimated viscosity ratios found in Section II of this report, and are shown as the lower two curves in Figures 21 and 22. The additional curves shown in these figures illustrate the effect on SMD of the higher viscosity JP-5 and Jet A fuels relative to the base JP-4 fuel of Section II. As shown, the effect of changing from the base JP-4 or JP-8 fuel to a relaxed specification JP-4 or JP-8 results in less of an effect on fuel nozzle atomization than changing from a base JP-4 fuel to JP-5 or Jet A. In addition, these curves also indicate the important effect of fuel temperature on atomization quality. As discussed in Section II, the viscosity ratio of a relaxed specification (higher viscosity) fuel to a base (lower viscosity) fuel increases with decreasing fuel temperature. This behavior is then reflected in the SMD ratio.



FD 176417

Figure 21. Sensitivity of Pressure-Atomizing Nozzle to Changes in Fuel Viscosity



FD 176418

Figure 22. Sensitivity of Airblast Nozzle to Changes in Fuel Viscosity

The ignition process is critically dependent on the evaporation of the fuel to produce a locally combustible mixture. Thus, the volatility of the fuel is of extreme importance. However, the rate of evaporation is also dependent on the fuel droplet size as well, smaller droplets having a larger surface-to-volume ratio and hence, for a given volatility, a higher evaporation rate. The effect of droplet size on the MIE is shown in Figure 24 (Reference 19). Figure 24 shows that for a given ignition energy, the lean ignition limit is extended toward a lower equivalence ratio with a decrease in droplet size. The volatility of the fuel is determined by the lighter weight constituents in the fuel. It may be quantified by measurement of the vapor pressure such as the Reid vapor pressure. However, this parameter may be misleading in that it is entirely an intensive parameter. A high vapor pressure does not necessarily mean that there is a sufficient quantity of volatile components in the fuel to form a combustible mixture. Inspection of the low end of the fuel distillation characteristics provides a better measure of a fuel's ignitability. Ignition results with several fuels obtained in a T63 engine combustor are shown in Figure 23 (Reference 20) plotted as the time to ignition vs primary zone equivalence ratio. In these results, the ignition time is correlated with the 25% distillation temperature.

Figures 23 and 24 indicate that the combined effects of increased SMD and decreased fuel volatility on ignition performance may be compensated for by increasing the combustor primary zone equivalence ratio. This may be accomplished by increasing the minimum scheduled fuel flow to the combustor. For example, the TF30-P-100 fuel control device has an external adjustment which is used to increase the minimum fuel flowrate by approximately 30% when changing from JP-4 to JP-5 operation.

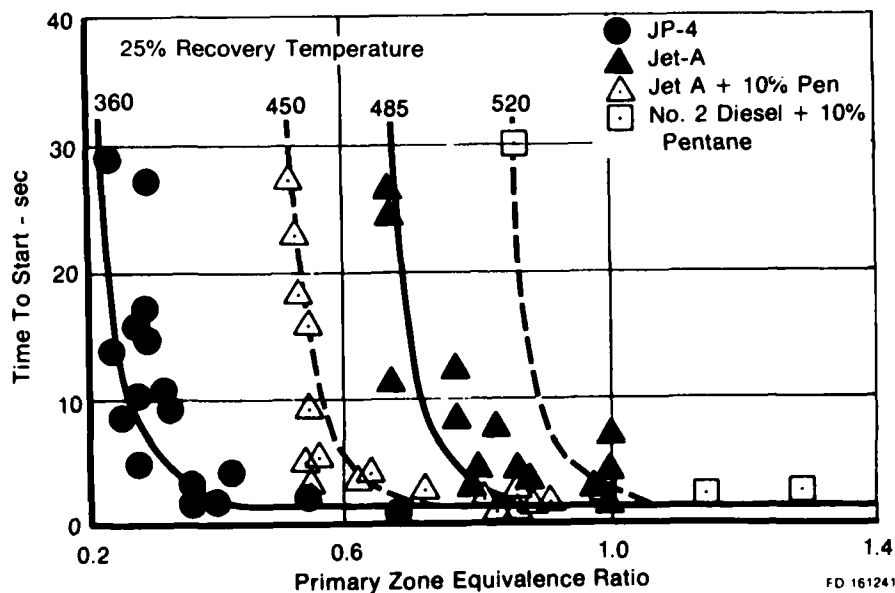


Figure 23. Effect of Fuel Volatility on Ignition of a T-63 Combustor

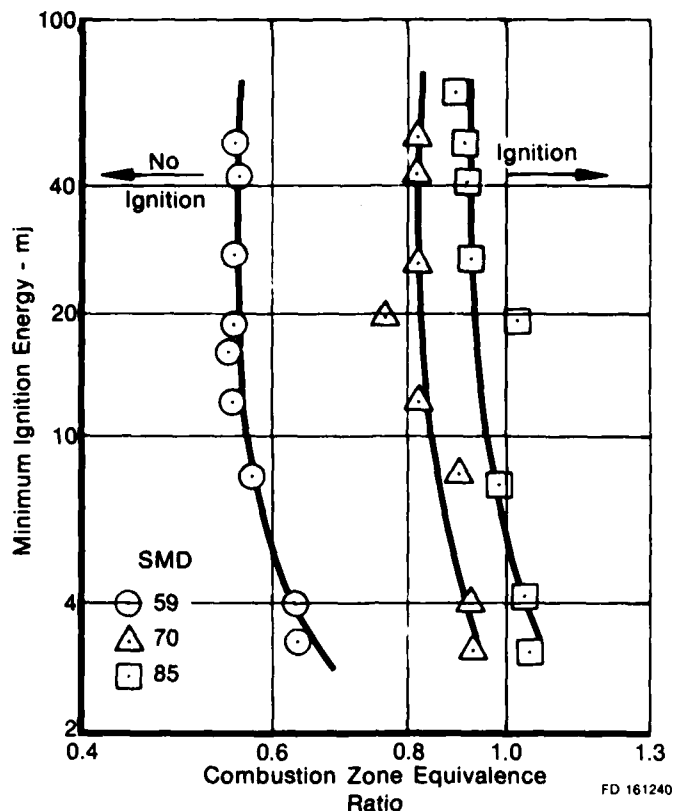
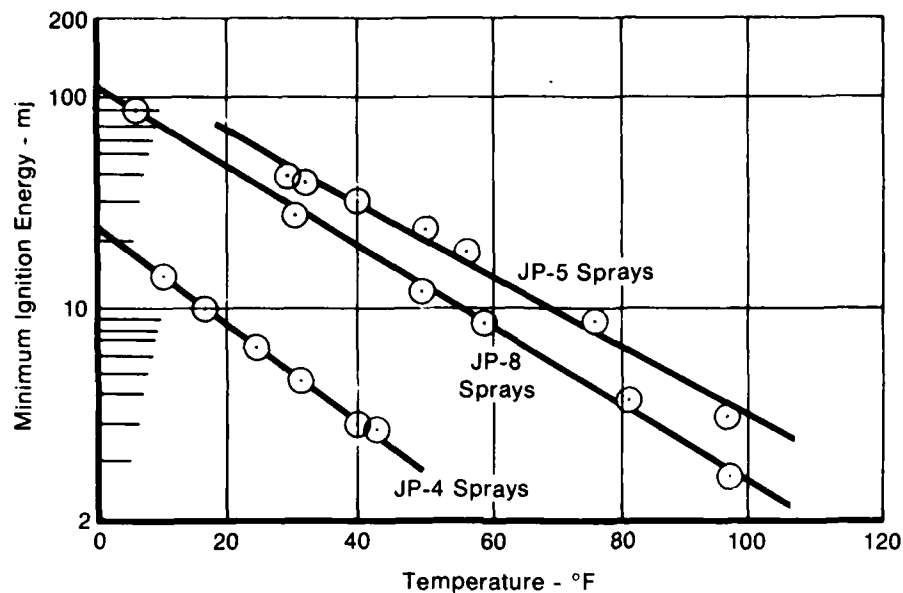


Figure 24. Effect of Atomization on Ignition

Fuel volatility also decreases for a given fuel with a decrease in fuel temperature, thereby reducing the rate of evaporation. The effect of fuel temperature on MIE at a constant droplet diameter is shown in Figure 25 for three fuels. The temperature coefficients of viscosity and volatility for hydrocarbon fuels ranging from JP-4 to No. 6 heating oil tend to be similar, and thus, temperature sensitivities of broadened-specification fuels are not expected to be dissimilar from current aviation fuels. However, as indicated in Figures 21, 22, and 25, temperature effects may become significant in those combustors whose altitude ignition performance is marginal with current fuels.

With regard to ignition characteristics, the sensitivity of the J57, TF30, and F100 combustors to changes in fuel properties is expected to vary because the performance of each combustor is a complicated interaction between these properties and the particular design characteristics. As discussed above, combustor characteristics which affect ignition performance are air velocity, turbulence level, primary zone equivalence ratio, and fuel nozzle type. The air velocity in each of these three combustors is dependent upon burner airflow distribution, swirl strength, and dilution jet location, and tends to increase in order from the J57 to the F100 combustor. The turbulence level in each combustor tends to increase with increasing liner pressure drop, and would, therefore, increase in order from the F100, to the J57, to the TF30. The primary zone equivalence ratio, under normal operating conditions, is highest in the F100 combustor and decreases in order from the TF30 to the J57. The fuel flow under engine startup and altitude windmilling conditions, however, is determined by the fuel control coupled to the high-pressure rotor speed. Thus, the actual primary zone equivalence ratio in a given combustor is dependent upon altitude and aircraft flight Mach number.



FD 156349

Figure 25. Effect of Temperature on the Ignition Energy for Fuel Sprays

The energy of the igniter system, and the relative position of the igniter are also crucial to combustor ignition characteristics. The F100 and TF30 igniter systems are 4 joules, while the J57 has a 20-joule system. The igniter placement in all three combustors is directly in line with a fuel nozzle; however, the J57 igniter is closer to the fuel nozzle than the TF30 and F100 combustors.

Differences in other combustor operating parameters, such as inlet temperature and pressure, between these three combustors become minimal under windmilling conditions where, instead, these variables are determined primarily by altitude and flight speed of the aircraft.

Whether the proposed changes in JP-4 and JP-8 fuel specifications considered in this study will impact the ignition performance of current AF engines is not clear. Due to the complex dependency of ignition performance on the various fuel-related and combustor-related variables discussed above, an accurate assessment of the impact of the relaxed-specification JP-4 and JP-8 fuels on the ignition characteristics of the F100, TF30, and J57 combustors is well beyond the scope of this discussion. However, a conservative estimate can be made for the F100 combustor based on test data with JP-4, JP-5, and JP-8 fuels.

Figure 26 presents the results of spooldown airstart tests using an F-15 aircraft operated on NATO F-34 (JP-8) fuel conducted as part of an Air Force-sponsored program (Reference 21). Spooldown airstart tests involve attempting an airstart at engine conditions at which the engine rotor following flameout has not deteriorated to that characterized by true windmilling conditions. The engine specification (CP 2903B) defines the airstart requirement at an engine high rotor speed of 40% of peak speed. Although all airstarts met the engine specification requirement, it was concluded that the use of JP-8 fuel will impose a 25-knot penalty (faster airspeed) in spooldown airstart capability relative to JP-4 fuel. Fuel temperatures to the

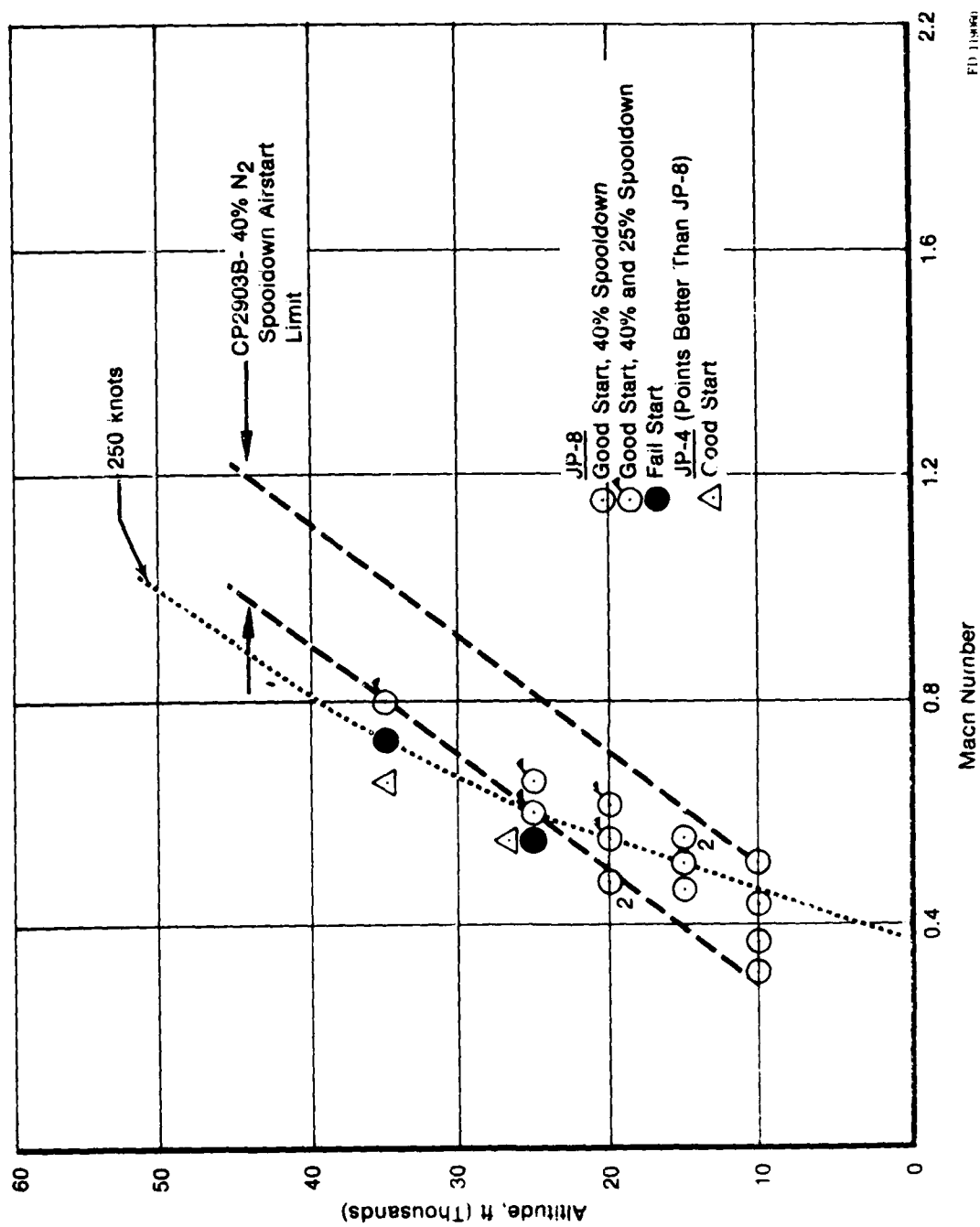


Figure 26. JP-8 Flight Test — Spooldown Airstarts (EAFB)

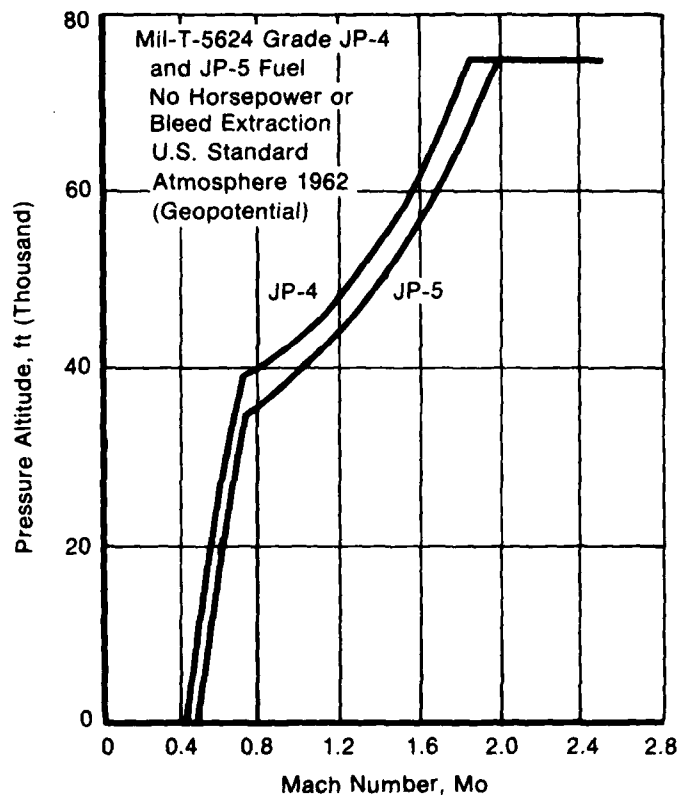
combustor during airstart conditions are normally greater than 100°F. Referring to Figure 21, the increase in SMD for Jet A relative to JP-4 is less than 10%. Since the viscosity of JP-8 fuel is essentially equal to that of Jet A, the 25-knot penalty in spool-down airstart capability for the F100 engine is primarily due to the decreased volatility of JP-8 relative to JP-4. Assuming that the increase in airspeed necessary for successful ignition is linearly proportional to the decrease in fuel volatility, and using the estimated 20% distillation temperatures found in Section II (Table 8) to indicate fuel volatility, the impact of the relaxed-specification fuels on spool-down airstart capability of the F100 is estimated to be as follows:

- The relaxed-specification JP-4 fuel will impose a penalty of 21 knots (or less) relative to the base JP-4 fuel
- The relaxed-specification JP-8 fuel will impose a penalty of 18 knots (or less) relative to the base JP-8 fuel.

Windmill airstarts at 10,000, 25,000, and 35,000 ft were also conducted during the Air Force-sponsored program referred to above. Results showed that specification requirements could be met with either JP-4 or JP-8 fuel. However, sufficient data was not obtained to allow a comparison of airstart limits with both fuels. The impact of the relaxed-specification fuels considered in this study on windmill airstart capability may be obtained using the information provided in Figure 27. This information was obtained from altitude ignition tests performed in an Altitude Simulation Test Facility at the Arnold Engineering and Development Center (AEDC) in Tullahoma, Tennessee during the development stages of the F100 engine. From Figure 27, the use of JP-5 fuel requires that the minimum airspeed necessary to obtain a successful windmill airstart be, on the average, approximately 40 knots higher relative to JP-4 fuel. It should be noted that the volatility of JP-5 fuel is less than either JP-8 or JP-4 fuels. The average 20% distillation temperature for JP-5 fuels is approximately 397°F (Reference 6) as compared to 233°F and 364°F for the base JP-4 and JP-8 fuels, respectively, used in this study. Proceeding on the same basis used for spool-down airstarts, the estimated impact of the relaxed-specification JP-4 and JP-8 fuels on F100 windmill airstart capability is as follows:

- The relaxed-specification JP-4 fuel will impose a penalty of 30 knots (or less) relative to the base JP-4 fuel
- The relaxed-specification JP-8 fuel will impose a penalty of 25 knots (or less) relative to the base JP-8 fuel.

Sufficient data could not be found to enable an estimate of the impact of the relaxed specification fuels on TF30 and J57 altitude ignition performance. A number of rig, engine, and flight tests have been performed with the TF30 and have verified that altitude ignition specifications can be met with both JP-4 and JP-5 fuels. However, these tests do not provide sufficient information to evaluate fuel effects on ignition for two reasons. First, the tests are normally performed to verify that specification limits can be met and not to determine the actual limits of the engine. Second, as mentioned previously, the minimum fuel flow is increased for engine operation on JP-5 (or JP-8) fuel relative to JP-4. Thus, while the altitude ignition performance of the TF30 may have different physical limits when operated on these higher viscosity/lower volatility fuels, the engine may not be functionally limited due to readjustment of the minimum fuel flow.



FD 176419

Figure 27. F100-PW-100 Turbofan Engine Nominal Mach Number and Altitude Limit for Windmill Airstart

It should be mentioned that cold-day ground starts will also be affected by broader specification fuels. In fact, the use of higher viscosity/lower volatility fuels is expected to have a greater impact on cold-day ground starts than on altitude ignition performance since fuel temperatures are lower (in flight, fuel is preheated before being pumped to the combustor). Sufficient data is not available at this time, however, to estimate the effect of the proposed relaxed-specification fuels on J57, TF30, and F100 ground-start capabilities.

Combustion Efficiency and Stability

Fuel property efforts on gas turbine combustion efficiency and stability have only recently been considered. Previously, these measures of combustor performance have been correlated with loading parameters which take into account the effects of pressure, airflow, temperature and equivalence ratio. One such parameter is the θ parameter (Reference 22):

$$\theta = P_{in}^{1.75} A_{rcr}^{0.75} / \dot{m} \exp (T_{in}/3000) \quad (21)$$

where, P_{in} and T_{in} are combustor inlet pressure and temperature, respectively, A_{rcr} and D_{rcr} are the maximum combustor cross-sectional area and width, and \dot{m} is the primary zone airflow rate. The use of this θ parameter will satisfactorily correlate variations in efficiency or lean stability limits which result from changes in any of the variables involved. By comparing these correlations between various combustors, such as the J57, TF30, and F100, the effect of combustor geometry may be deduced.

An example of the use of the θ parameter is shown in Figure 28 (Reference 23) where efficiency curves for a disk stabilized combustor using two different fuel nozzles are compared with efficiencies using three different fuels in a T63 combustor equipped with a dual-orifice pressure-atomizing nozzle. Figure 28 illustrates the validity of the parameter in correlating efficiency data from different combustors. This figure reveals a deficit in efficiency with increased fuel droplet size, and a decrease in efficiency with decreased volatility and increased viscosity. Similar effects on stability were also shown in Reference 23.

Figure 28 also illustrates that fuel properties do have a significant impact on efficiency. These fuel property effects on the overall combustor performance are exerted through the processes of fuel-air mixture preparation, droplet ignition, and combustion which occur in the primary zone of the combustor. The processes occurring in the primary zone are extremely complex, but can be characterized by a combustion time which, for direct liquid-injection combustors, involves droplet evaporation, gaseous diffusion, and chemical reaction. This characteristic time is not simply the sum of times for three successive processes since diffusion and reaction occur before droplet evaporation is complete, but is usually determined by one of the processes which, for a given system, is the slowest. It is through these individual characteristic times that fuel property effects become evident.

Various attempts have been made to split regions of gas turbine combustor operation into areas in which one of these times is the determining factor in the overall combustion process. Figure 29 (Reference 24) is an example of such an attempt where initial droplet size is used as the determining factor. The particular droplet sizes subdividing the various modes are not necessarily applicable to the J57, TF30, and F100 combustors. However, the droplet size is an important parameter in the correlation of efficiency and stability data.

Ballal and Lefebvre (Reference 25) have similarly defined these characteristic times and have used the initial droplet diameter as the controlling parameter. A critical droplet diameter, D_{crit} , has been defined such that above this value droplet evaporation is the limiting process. The use of this model shows particularly well the effects of fuel properties and fuel nozzle characteristics on combustion efficiency. As discussed in connection with the ignition mechanism, the droplet size is dependent upon nozzle type and the viscosity and surface tension of the fuel. Figure 30 (Reference 25) shows this variation in D_{crit} with droplet evaporation time for several different fuels. The critical droplet size is dependent upon fuel properties, combustor flow, and combustor geometry characteristics. The evaporation time is determined by combustor characteristics such as airflow distribution, flow velocities, and swirl strength. If, for example, the droplet evaporation time is 4 milliseconds (ms), D_{crit} for kerosene is 36 microns and for light fuel oil is 27 microns; the difference, 15 microns, is due to viscosity, density, and volatility differences. Thus, to maintain the same evaporation performance, the atomization of the fuel would have to be 33% better for the fuel oil than for kerosene. In reality, however, the performance of the fuel nozzle, particularly pressure-atomizing nozzles, would deteriorate due to the change in fuel properties, indicating an even greater dependence of the overall combustion process on droplet evaporation. Airblast fuel nozzles, such as used in the F100 combustor, are less sensitive to fuel viscosity, and thus, tend to minimize deterioration in droplet size with increased viscosity. However, even if droplet size were constant for the above example, the decreased volatility would require evaporation time that is 50% longer than for kerosene.

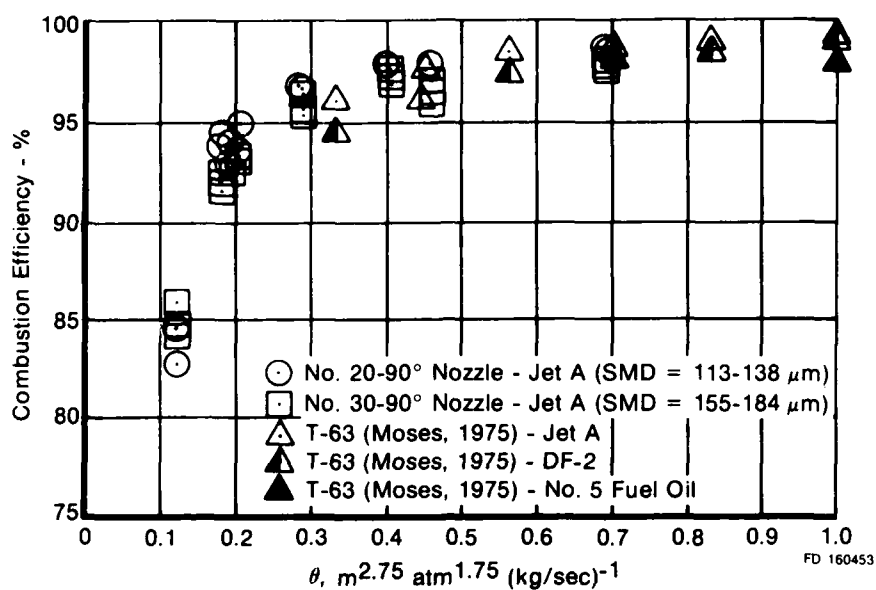


Figure 28. Combustion Efficiency vs θ for a Disk-Stabilized Combustor ($D_{ref} = 14.6$ cm, $A_{ref} = 167.5$ cm²) and a T-63 Combustor ($D_{ref} = 15.2$ cm, $A_{ref} = 182.4$ cm²)

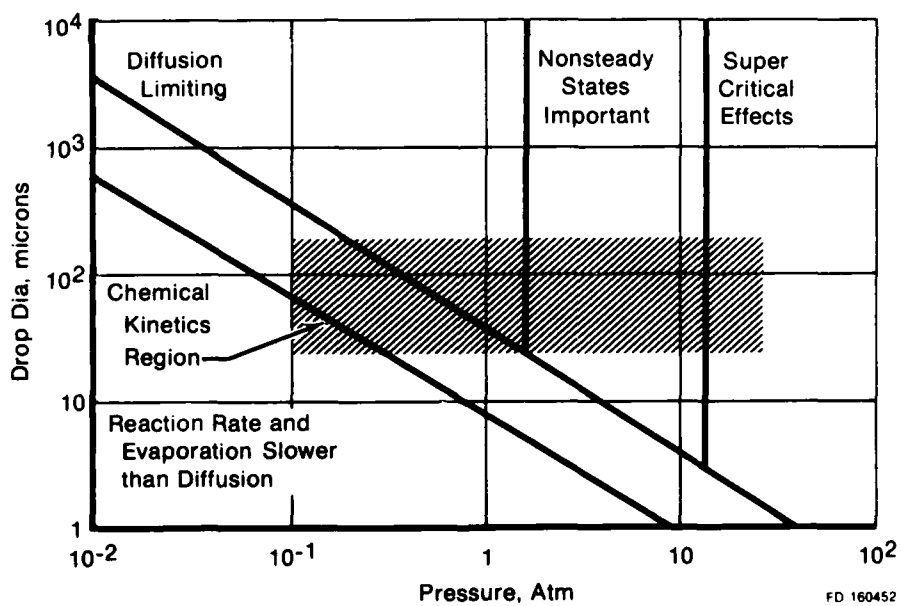


Figure 29. Physical and Chemical Control of Combustor Processes

The variation in critical droplet diameter with evaporation time, shown in Figure 30, is dependent upon the combustor inlet temperature and pressure. The curves presented here are at one atmosphere and 80°F. As pressure and temperature increase, the values for D_{crit} increase. At 890°F and 300 psi, D_{crit} for kerosene is approximately 100 microns. This indicates that the effects of fuel viscosity and volatility are more evident at low-power conditions. The variation in D_{crit} with engine compressor pressure ratio is given in Figure 31 (Reference 25). Table 10 shows the compression ratio for the J57, TF30, and F100 combustors at Idle and SLTO power conditions. From this tabulation and the results of Figure 31, several conclusions can be drawn. First, the effect of physical properties of the fuel on efficiency is more evident at lower power operation. Also, droplet evaporation should not be the limiting process for the TF30 and F100 combustors at high-power conditions. The J57 combustor is the most likely of the three to be affected by increased viscosity and decreased volatility of the fuel.

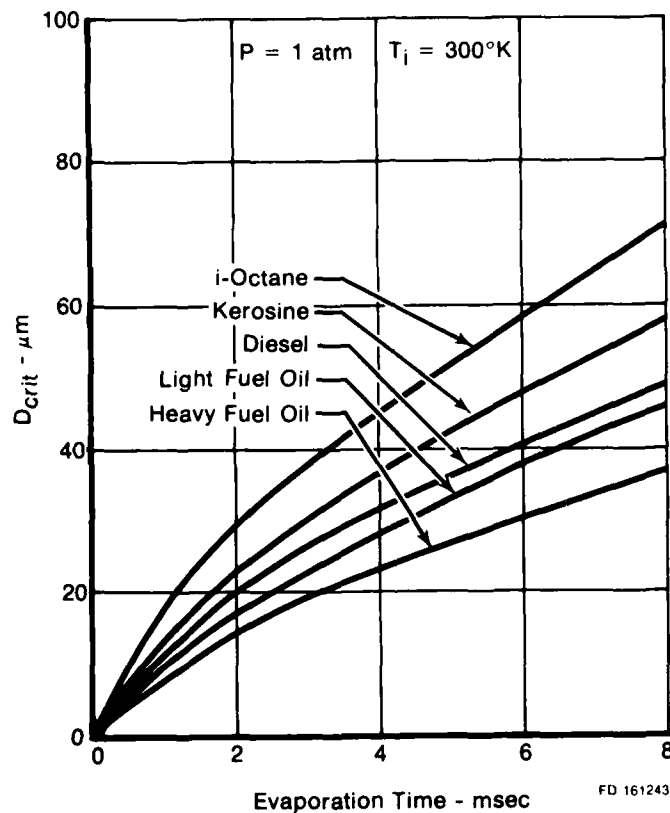


Figure 30. Influence of Evaporation Time on Critical Mean Drop Diameter

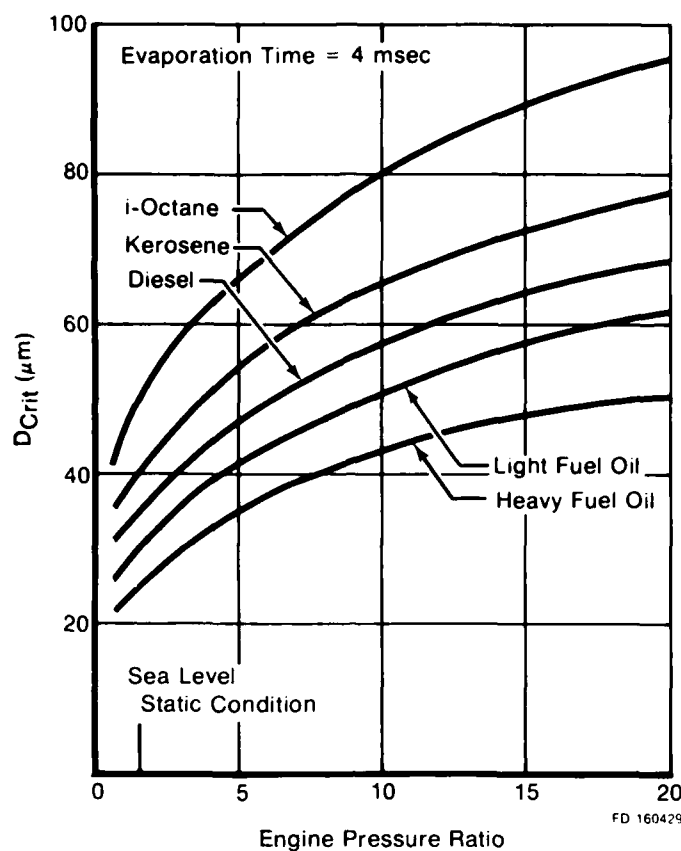


Figure 31. Influence of Pressure on Critical Mean Drop Diameter

TABLE 10. COMPRESSION RATIO AT IDLE AND SLTO THRUST SETTINGS

Engine	Idle		SLTO	
	$P_{\text{compressor}}$	$\text{Exit}/P_{\text{ambient}}$	$P_{\text{compressor}}$	$\text{Exit}/P_{\text{ambient}}$
J57		2.6		12.2
TF30		3.5		19.0
F100		4.3		24.9

The values of D_{crit} , shown in Figures 30 and 31, are also dependent on the turbulence intensity in the primary zone of the combustor. The curves were generated assuming a turbulence intensity of 20%. The effect of turbulence is to increase the rate of evaporation, and hence, deficits in fuel volatility can be compensated for to some extent by turbulence. For kerosene, D_{crit} is increased by 30 microns for variation in turbulence from zero to 30%. The level of turbulence in most gas turbine combustors is poorly defined, but is dependent upon swirl strength and airflow distribution in the primary zone of the combustor. The combustor liner pressure drop is the driving force for mixing in the primary zone, and hence, is related to turbulence intensity. The liner pressure drop for the J57, TF30, and F100 combustors is compared in Table 11. Based on the liner pressure drop, the turbulence intensity should decrease from the TF30 to the F100 combustors, thus increasing the sensitivity to fuel volatility in that order.

TABLE 11. COMBUSTOR LINER
PRESSURE DROP

Combustor	Liner Pressure Drop at SLTO Condition ($\Delta P/P$, %)
J57	2.3
TF30	2.8
F100	1.4

The relative effects of fuel viscosity and volatility on combustion efficiency are shown in Figure 32 (Reference 25). In this case the basis of comparison is the combustion efficiency with kerosene. Deviations from this value relate to density, surface tension, viscosity, and volatility for evaporation-limited systems. The relative effects of volatility and viscosity are shown by calculating the efficiency ratio assuming constant droplet diameter. This curve is shown in Figure 32. Since the effect of viscosity is dependent on the nozzle type, nozzle characteristics must be incorporated into the calculation of efficiency. In the pressure-atomizing swirl nozzles of the J57 and TF30, the viscosity exponent is 0.20 compared to 0.13 for the F100 airblast nozzle. With these viscosity exponents, the relative efficiency curves for J57, TF30, and F100 nozzles are shown in Figure 32. The deviation of efficiency from unity along the constant droplet diameter curve is a measure of fuel viscosity, while the deviation between this curve and curves for airblast and pressure-atomizing nozzles is reflective of viscosity effects.

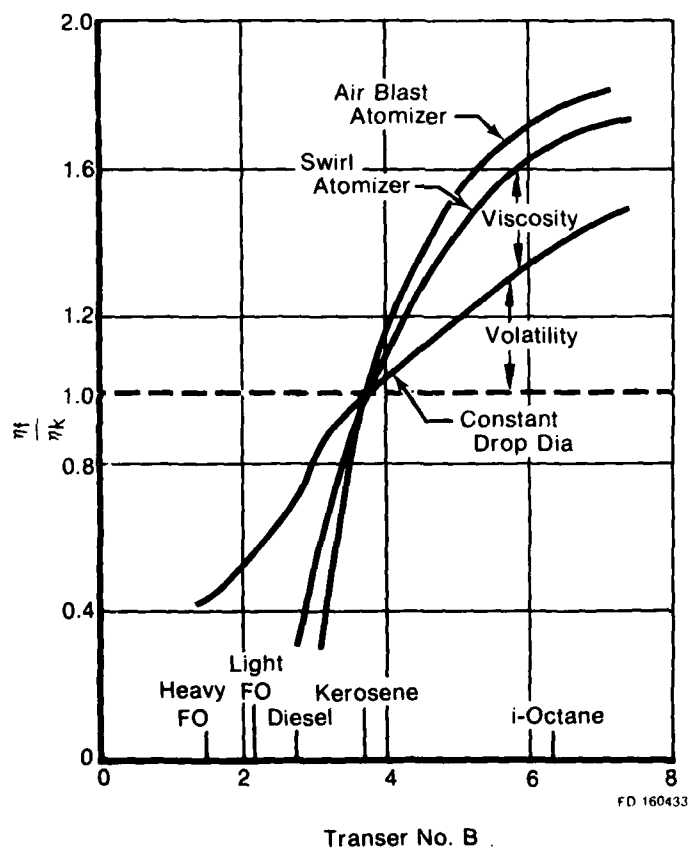


Figure 32. Effect of Mass Transfer Number on Combustion Efficiency

Other attempts to incorporate fuel and nozzle effects into correlations of efficiency and stability have been made by Colket et alia (Reference 26). The basic model of the combustion process consists of droplet evaporation, mixing, and chemical reaction, each process being described by characteristic times. The droplet lifetime, which is proportional to droplet diameter and evaporation rate, incorporates both fuel properties and nozzle characteristics. However, attempts to correlate efficiency data from a disk-stabilized combustor burning a variety of fuels revealed an apparent independence of efficiency on nozzle and fuel type. Instead, efficiency was correlated with a parameter proportional to mixing time. In these experiments, combustor inlet temperature ranged from 450 to 980°F. A stability criterion involving mixing time, droplet lifetime, fuel ignition delay, and burning time is suggested; however, sufficient data was lacking to develop a successful correlation.

The complex interaction of the processes of evaporation, mixing and chemical reaction, and the effects that fuel properties have on the overall process may be somewhat simplified by considering premixed combustion systems. Under the Air Force Low Power Emission Program (Reference 27), a premixed combustor design was evaluated with both JP-4 and JP-5 fuels. The carbon monoxide, CO, and unburned hydrocarbons, UHC, were measured over a range of fuel-air ratios and with an inlet temperature of 400°F at atmospheric pressure. Since combustion efficiency is readily related to these emissions, the higher levels of CO and UHC found in this test with JP-5 fuel represent a deficit in efficiency due to the reduced volatility of JP-5. The lean stability limit with JP-5 fuel was lower than that with JP-4 which is accounted for in terms of the volatility difference of these fuels.

In the NASA Experimental Clean Combustor Program, Fuels Addendum (Reference 28), the effect of fuel volatility and viscosity on combustion efficiency is also evident. This portion of the program evaluated two advanced, low-emissions combustors on Jet A, diesel, and home heating oil. The Vorbix combustor was a two-stage burner with a primary zone similar to the J57, TF30, and F100 combustors. At power settings greater than Idle, both fuel zones were operative. At the SLTO condition there was no change in efficiency with fuel property variations. At Idle however, the combustion efficiency decreased slightly for the diesel and No. 2 fuel oil.

As will be discussed in the following section, the proposed changes in this study are expected to have a minimal impact on combustion efficiency of the J57, TF30, and F100 combustors. However, with regard to broadened-specification fuels in general, several conclusions concerning factors which influence efficiency can be drawn. First, the sensitivity of an individual combustor's performance in this area is dependent upon design features, such as turbulence intensity and fuel nozzle type, and on operating conditions, such as combustor inlet temperature, pressure, and fuel-air ratio. The lower the temperature and pressure, the more susceptible the combustor will be to fuel type. Thus, the J57 should exhibit more dependence on fuel type than the TF30 and F100. Fuel nozzles whose performance is sensitive to fuel viscosity will result in a greater dependence of efficiency on fuel properties. In this case, the J57 and TF30 combustors should exhibit greater sensitivity to fuel properties than the F100. Small deficits in fuel volatility can be compensated for by a higher turbulence intensity. This indicates that efficiency sensitivity to fuel type should increase in the order of TF30, J57, and F100.

Finally, if the fuel does impact the combustion efficiency, it does so through the same properties which affect ignition: primarily viscosity and volatility. Variations in molecular structure of hydrocarbon fuels from petroleum sources do not alter ignition delay times and chemical reaction rates sufficiently to make these processes limiting in the overall combustion process.

Based on the difference in combustor operating conditions, the sensitivity of combustion efficiency to fuel property effects should be greater in the J57 than the TF30 and F100 combustors.

Despite the strong influence of fuel viscosity and volatility on emissions of CO and UHC, most investigators have chosen to correlate this data with hydrogen content, as shown in Figures 33 and 34 (References 30 and 31). The data illustrated in Figure 33 were obtained from tests with a General Electric J79-17A combustor. As shown, emissions levels are low at high-power conditions, as is the case for the majority of conventional combustors including the J57, TF30, and F100. As expected, the variation of CO emissions with fuel hydrogen content is greatest at idle conditions, and decreases with increasing power levels. Figure 34 illustrates the variance of both CO and UHC emissions with hydrogen content for a P&WA JT8D combustor at idle conditions. A comparison of Figures 33 and 34 reveals that both combustors exhibit approximately the same sensitivity of CO emissions to hydrogen content. This is not unexpected since both combustors are of similar configuration (can-annular with pressure-atomizing nozzles) and have similar operating conditions at idle.

The information in Figure 34 can be used to estimate the impact of the proposed fuel property changes under consideration in this study on combustion efficiency. In Section 2 of this report, the change in hydrogen content predicted for Case 2 was found to be 0.24 for the relaxed JP-4 fuel, and 0.25 for the relaxed JP-8 fuel. Based on these values, Figure 34 indicates an increase in both El_{CO} and El_{UHC} of approximately 0.5 lb_m/1000 lb_m. Although idle operating conditions of the JT8D more closely resemble those of the TF30 than the J57, the small magnitude of the predicted change in El_{CO} and El_{UHC} indicates that the relaxed-specification JP-4 and JP-8 fuels will have a negligible impact on either engine. As mentioned above, the F100 combustor is expected to exhibit less sensitivity of combustion efficiency to fuel property changes than the TF30 and J57 combustors. Therefore, the proposed changes in JP-4 and JP-8 specifications should also have a negligible impact on the efficiency of this combustor also.

Emissions

CO And UHC Emissions

Carbon monoxide and unburned hydrocarbon emissions are indicative of the efficiency of the combustion process. In fact, the most reliable means of determining combustion efficiency, η_c , is by using CO and UHC emission data according to the equation (Reference 29)

$$\eta_c = 100 - \frac{(El_{CO}) Q_{CO} + (El_{UHC}) Q_{UHC}}{Q_{fuel}} \times 10 \quad (22)$$

where El_{CO} and El_{UHC} represent the emission indices of CO and UHC in pounds of emission per 1000 pounds of fuel, and Q refers to the lower heating value of the particular species. Thus, the combustor design features and operating conditions, as well as fuel properties, which affect combustion efficiency are, by definition, those which affect CO and UHC emissions also.

As with combustion efficiency, the fuel properties affecting CO and UHC emissions are primarily volatility and viscosity. These fuel properties will have the most significant effect at engine idle conditions when fuel temperatures and combustor pressures and temperatures are relatively low. A comparison of combustor operating conditions at engine idle for the F100, J57, and TF30 combustors is shown in Table 12.

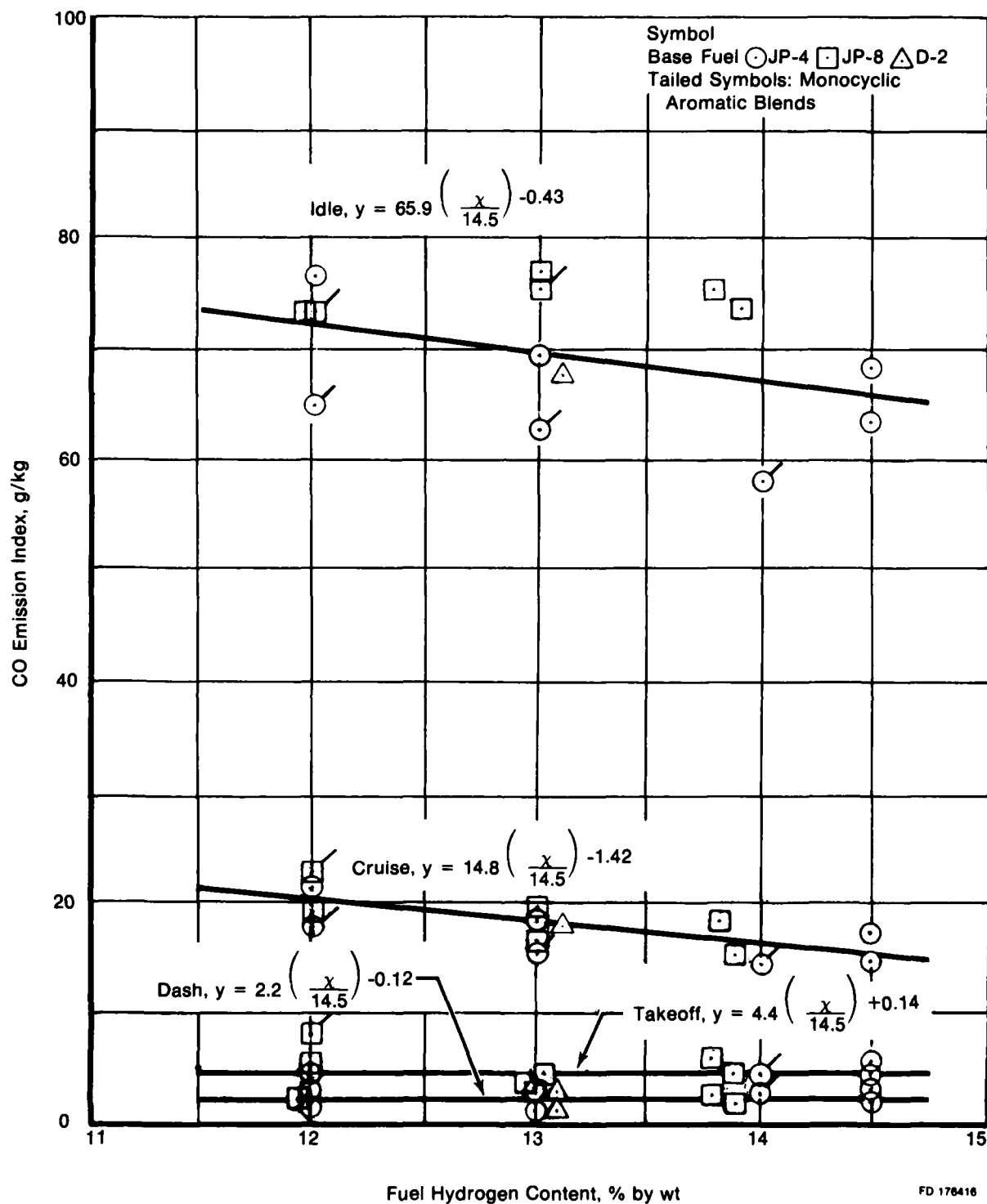


Figure 33. Correlation of CO Emission Index With Fuel Hydrogen Content at Various Engine Conditions

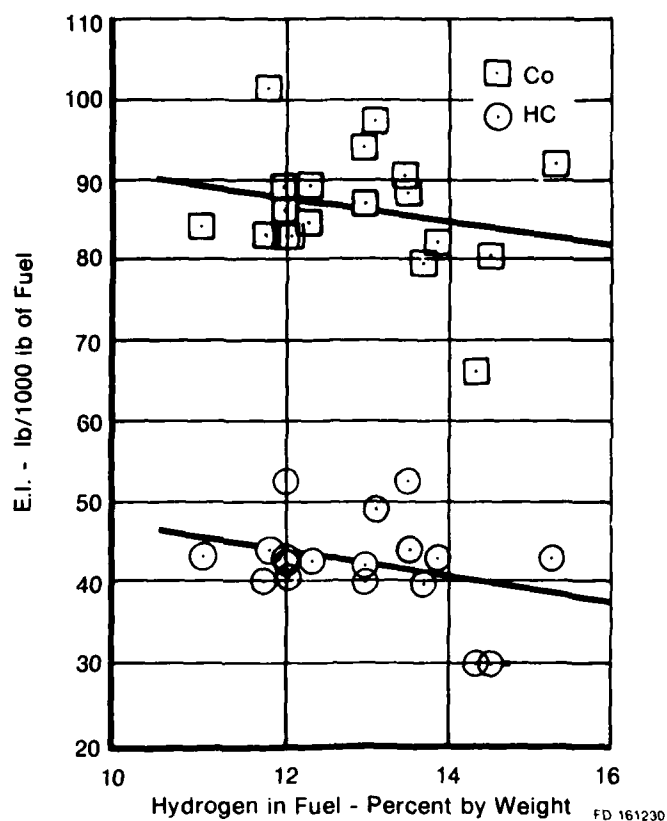


Figure 34. Effect of Hydrocarbon Content of Fuel on Emissions of CO and Unburned Hydrocarbons at Idle Conditions

TABLE 12. ENGINE COMBUSTOR OPERATING CONDITIONS AT IDLE CONDITIONS

Combustor Parameter	Engine		
	J57	TF30	F100
T _{IN} (°F)	194	345	428
P _{IN} (psia)	38	52	63
t/a (lb _m /lb _m)	0.0050	0.0153	0.0093

NO_x Emissions

The nitrogen oxide emissions, NO_x, formed from atmospheric N₂, are controlled by the chemical kinetics of the many reactions that occur within the combustor. One of the first attempts to explain the mechanism of NO formation was that of Zeldovich (Reference 32) who proposed:



Both reaction (1) and the reverse of the (2) have large activation energies which result in a strong temperature dependence for NO formation.

This reaction mechanism has been expanded and refined to better understand and correlate NO_x emissions from combustion sources. However, the situation is still not completely resolved in the case of fuel-rich combustion ($\phi > 1.2$). In this case, predicted values of NO are lower than experimental values. Several studies (References 33 and 34) have shown that in fuel-rich hydrocarbon-air combustion, the formation of NO_x is dependent on the concentration and molecular structure of the hydrocarbon species. The implication of this result is that the hydrocarbon fuel, or a fragment of the original hydrocarbon, plays an active role in NO_x formation. Thus, the nature of the hydrocarbon fuel is significant in NO_x formation not just that its combustion provides the high temperature medium for the N₂ - O₂ reactions, but through its chemical structure, enters into the reaction mechanism which forms NO_x.

Consideration of the detailed mechanisms whereby the hydrocarbon species enter the N₂ - O₂ reaction scheme is beyond the scope of this discussion; however, the effect of classes of hydrocarbon fuels on the flame temperature is readily seen in Figure 35 (Reference 35). This shows the nondissociated equilibrium flame temperature as a function of carbon number for several different classes of compounds including paraffinic, olefinic, and single-ring and double-ring (naphthalenes) aromatics. On this basis alone, NO_x emission should increase in the same order.

The trend toward higher flame temperatures, shown in Figure 35, with changes in hydrocarbon classes is paralleled by an increase in the number of double bonds in the hydrocarbon or by a decrease in the number of hydrogen bonds. Thus, hydrogen content correlates with flame temperature in an inverse manner. This change in flame temperature with hydrogen content of the fuel is evident in NO_x emissions from gas turbine combustors. Figure

36 (Reference 31) shows an approximate 12% increase in NO_x emissions from a JT8D combustor operated at SLTO conditions when hydrogen content decreased from 14 to 12%. These data were generated from combustor testing using Jet A fuel and blends with Jet A of various single- and multiple-ring aromatic compounds. The inlet temperature at the Cruise condition for the JT8D is 660°F, while at SLTO it is 825°F. Since the peak temperature acquired in the combustor is dependent on the inlet temperature, the effect of small changes in the hydrogen content of the fuel are not sufficient to raise the flame temperature sufficiently to enhance NO formation at lower power conditions. Thus, NO_x increases with decreased hydrogen content are expected only at the high-power conditions.

The sensitivity of the NO_x emissions of the J57, TF30, and F100 combustors is expected to be dependent on the operating conditions of these combustors. Table 13 shows the combustor inlet temperature, pressure, and average exit temperature for each of these three combustors at the SLTO condition. Despite the wide variation in inlet and exit combustor temperatures from the J57 to F100 combustor, there is droplet or diffusion burning resulting in stoichiometric flame temperatures for some period of time in the primary zone of each of these combustors. However, the trend exhibited by the combustor inlet and exit temperatures is expected to be paralleled by the NO_x emission sensitivity to hydrogen content of each of these combustors.

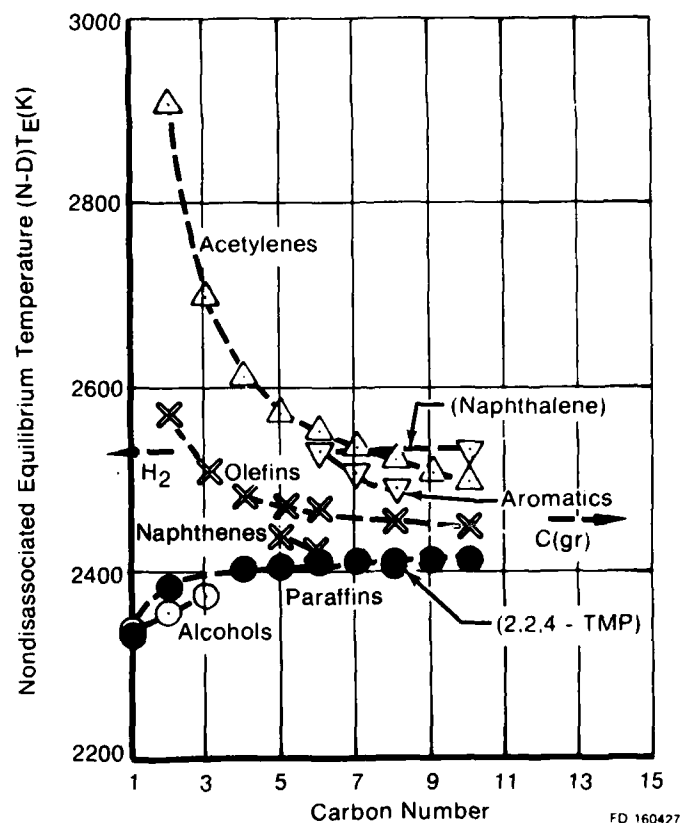


Figure 35. Calculated Stoichiometric Equilibrium Temperatures; Fuel and Products in Gaseous Phase

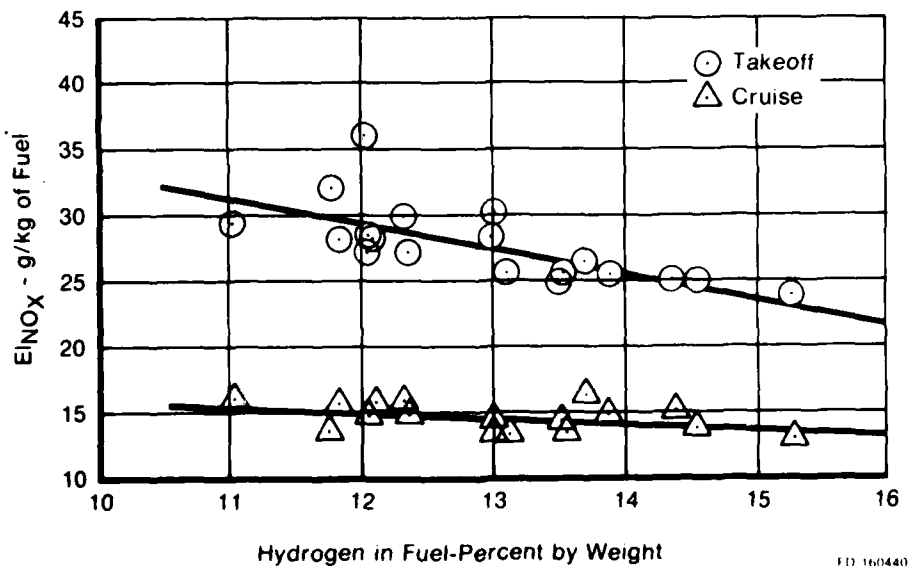


Figure 36. Effects of Hydrocarbon Content of Fuel on NO_x Emissions at Takeoff and Cruise Conditions

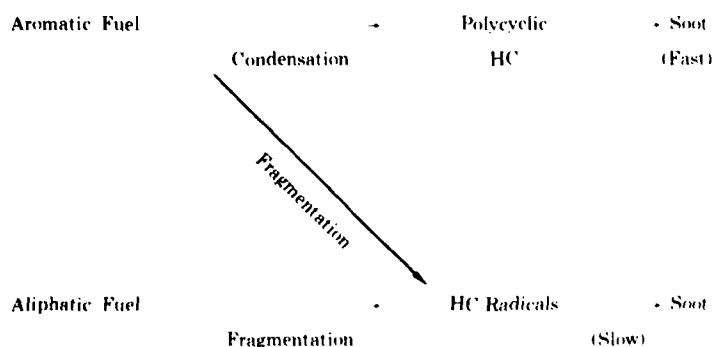
TABLE 13. ENGINE COMBUSTOR OPERATING CONDITIONS AT THE SLTO THRUST SETTING

Combustor Parameter	Engine		
	J57	TF30	F100
T _{IN} (°F)	680	923	1,004
T _{EXH} (°F)	1,580	2,250	2,568
P _{IN} (psia)	180	280	305

Smoke and Soot Formation

Soot formation in a gas turbine combustor is evidenced in the form of visible smoke in the exhaust. Smoke is a relatively pure form of carbon particulates whose size is approximately one micron or less. Carbon particulates of this size form aerosols whose motion is determined by the gross flowpath streamlines in the hot section of the engine and do not result in erosion of turbine airfoil surfaces. In this form, carbon particulate emissions are primarily an aesthetic nuisance. However, the path of carbon particulates of approximately 20-25 microns in size through the turbine vane and blade rows is no longer along the gas path streamlines due to sufficient particle momentum. The subsequent impact of these particulates on the airfoil surface can erode the surface coating, exposing the base material to oxidation and corrosion. The formation of carbon particulates is dependent, both on the aerodynamics of the combustor flow field and on the chemical nature of the fuel.

The chemical mechanism for the formation of carbon particulates is not yet completely understood. However, there appears to be two mechanisms whereby soot can be formed which differ in their kinetic rates depending, in part, on the chemical structure of the fuel. A simplified mechanism is shown below (References 36 and 37):



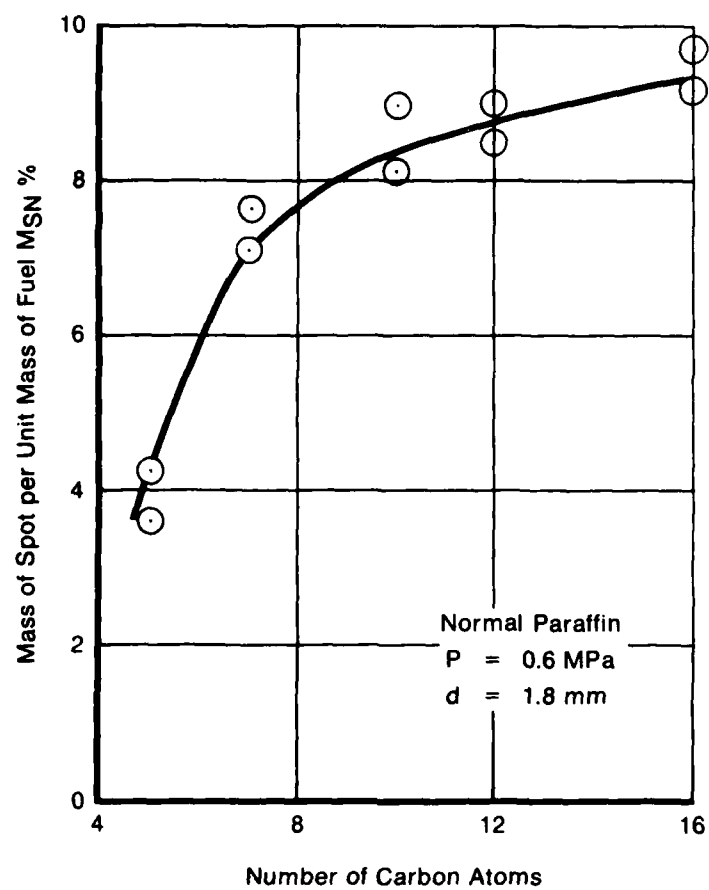
The paths to soot formation proceed either through pyrolytic fragmentation of the fuel molecules or a condensation reaction involving only aromatic fuel molecules. The fragmentation reaction appears to be kinetically slower than the condensation reaction. Soot formation from aliphatic fuel molecules must proceed through the fragmentation route, whereas with aromatic molecules, the formation can occur through a fast route (condensation) or through a slower route (fragmentation). The controlling factor with aromatic species appears to be temperature, with the faster condensation mechanism favored below 2800°F.

The soot-forming tendencies of many hydrocarbon fuels have been studied in premixed laboratory flames for many years. These results generally show the soot formed as a function of the location in the flame and the oxygen to carbon ratio in the reactant mixture. Figure 37 (Reference 38) shows the variation in soot formed at a given location in the flame as a function of the number of carbon atoms in the fuel molecule for a series of straight-chain paraffinic hydrocarbons. It is evident from this figure that the tendency of paraffinic hydrocarbons to form soot greatly increases with carbon number up to approximately 8 carbon atoms and then tends to level off. A more detailed view of intermediate species formed in a soot-producing methane-oxygen flame is shown in Figure 38 (Reference 39). This shows the concentration profiles of several species as a function of height above the burner for two different $\text{CH}_4 - \text{O}_2$ feed ratios. These results indicate that ethane is first formed from methane in the reaction zone, and that ethylene and acetylene are subsequently formed through dehydrogenation with the peak acetylene concentration occurring in the oxidation zone. This result also shows the formation of aromatic species formed in the soot-producing $\text{CH}_4 - \text{O}_2$ flame, suggesting that even with paraffinic fuels, aromatic intermediate species play a role in the soot-formation mechanism.

If aromatic species are indeed involved to some extent in soot formation with paraffinic fuels, then it seems reasonable that the formation of soot would proceed more readily with fuels of a high aromatic content. Blazowski (Reference 40) has categorized various fuels into three groups depending upon their sooting tendencies. Stereotypical of each of these groups are ethylene, toluene, and methylnaphthalene, which exhibit increasing sooting tendencies in that order. The data in this report were obtained in a premixed, jet-stirred reactor, which was an attempt to simulate the strongly back-mixed conditions in the primary zone of a gas turbine combustor. Figure 39 shows the soot production of some of these compounds as a function of equivalence ratio at a combustor inlet temperature of 570°F . From the figure it is evident that the tendency to form soot is significantly greater for methylnaphthalene than for the other compounds tested.

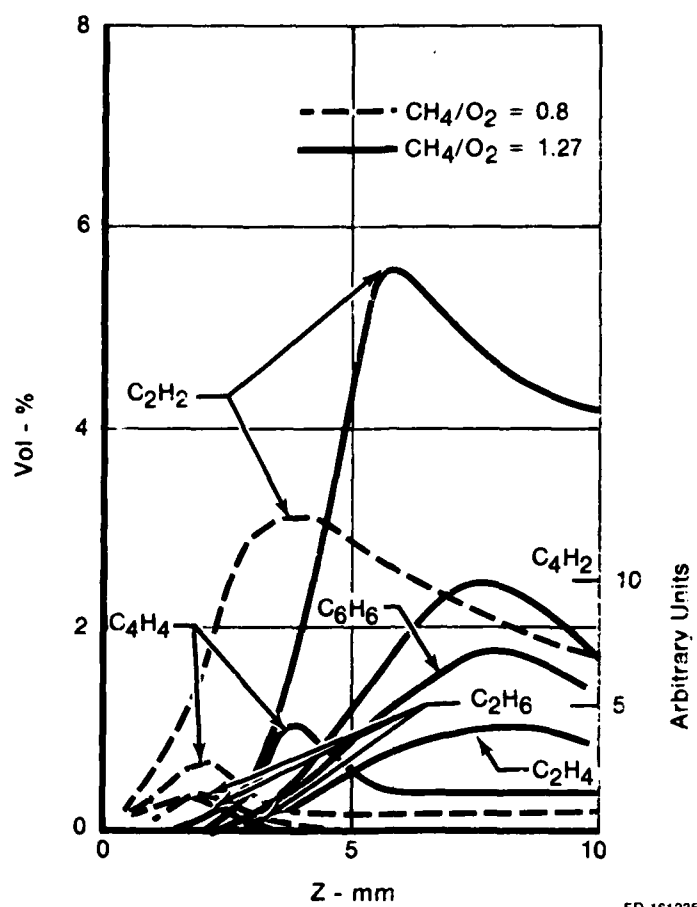
Aside from considerations of the chemical nature of the hydrocarbon fuel, the method of injecting the fuel into the combustion chamber and the mixing that occurs there is extremely important in determining if soot is formed. For example, the equivalence ratio for incipient soot formation for a toluene-air mixture in a laminar, flat flame is 1.34. Under intensely back-mixed conditions, the equivalence ratio can be raised to 1.50 without soot formation (Reference 40). The effect of mixing and fuel atomization is shown in Figure 40 (Reference 41) for a kerosene-air mixture in a swirl-stabilized combustor. This figure illustrates soot production as a function of equivalence ratio for a premixed-prevaporized flame and for several cases of direct fuel injection, where fuel droplet size was approximately 50 microns. The soot formation from the premixed-prevaporized flame appears to represent some limiting value, at least for this particular combustor. In this case, there is little soot formed below an equivalence ratio of approximately 1.3. Above 1.3, however, soot is formed readily. Also, with the premixed-prevaporized fuel-air mixture, there appears to be little effect of pressure over the range of these data. For the case of direct fuel injection, soot is formed at equivalence ratios less than unity, and there appears to be more scatter in the data with pressure variation.

Another investigation of the sooting tendencies of kerosene and the effect of fuel atomization was done by Prado et al. (Reference 43). In this case, fuel was injected with an air-assist nozzle directly into a swirl-stabilized combustor. By varying the air-assist pressure to the fuel nozzle, the atomization of the fuel was varied. While no estimate of droplet size was made, varying the air-assist pressure from 15 to 20 psig caused a 50% variation in soot produced at a given distance from the nozzle, with the minimum soot production corresponding to the highest air-assist pressure. A comparison of the soot produced from kerosene and two aromatic fuels is shown in Figure 41. For these conditions, the aromatic fuels yield nearly an order of magnitude greater soot production than kerosene.



FD 160439

Figure 37. Effects of Kinds of Fuels on Soot Formation



FD 161235

Figure 38. Vertical Concentration Profiles of Gas Species in a Flame for Different CH_4/O_2 Feed Ratios

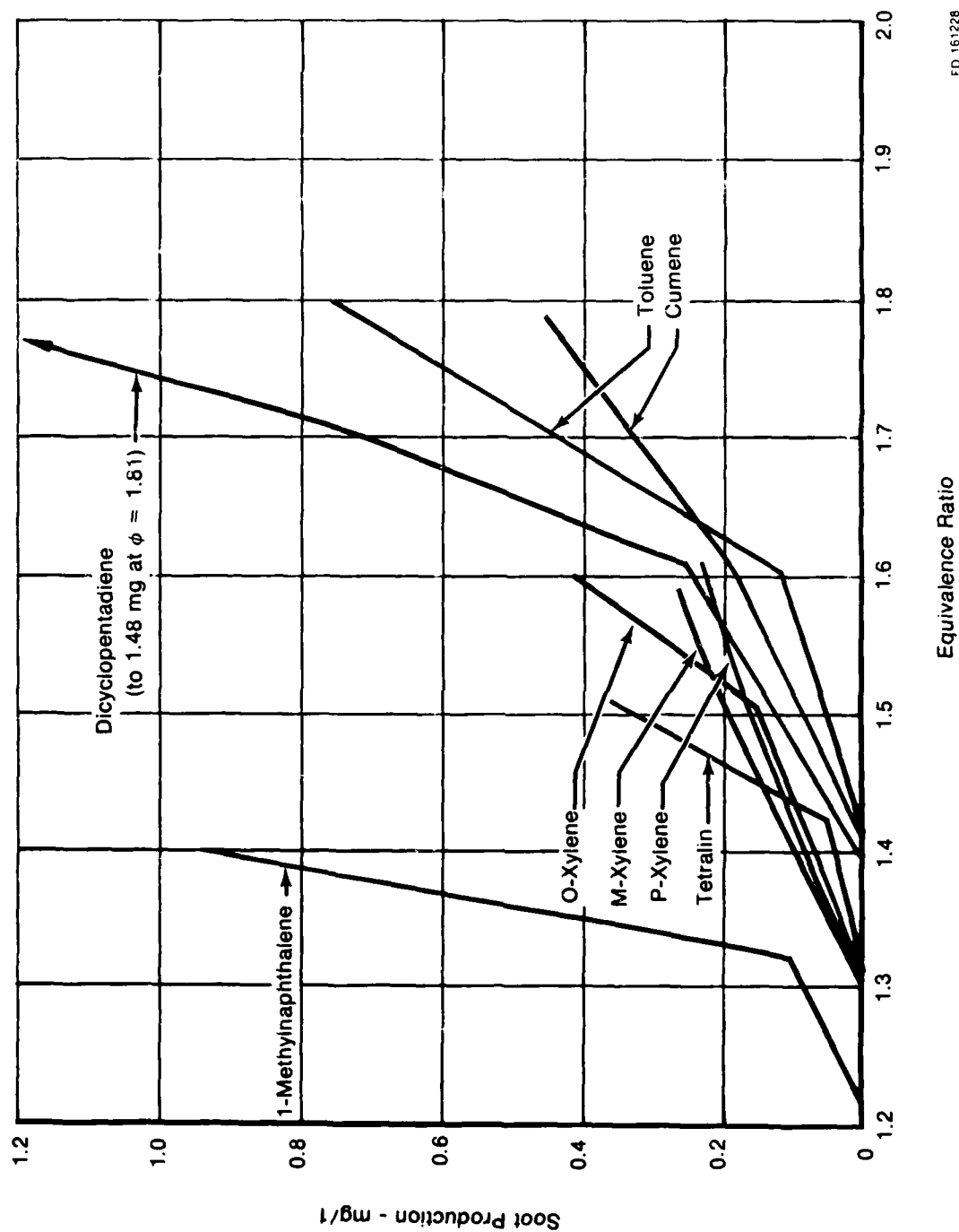


Figure 39. Soot Formation for Various Pure Hydrocarbons at 570°F Inlet Temperature

FD 161228

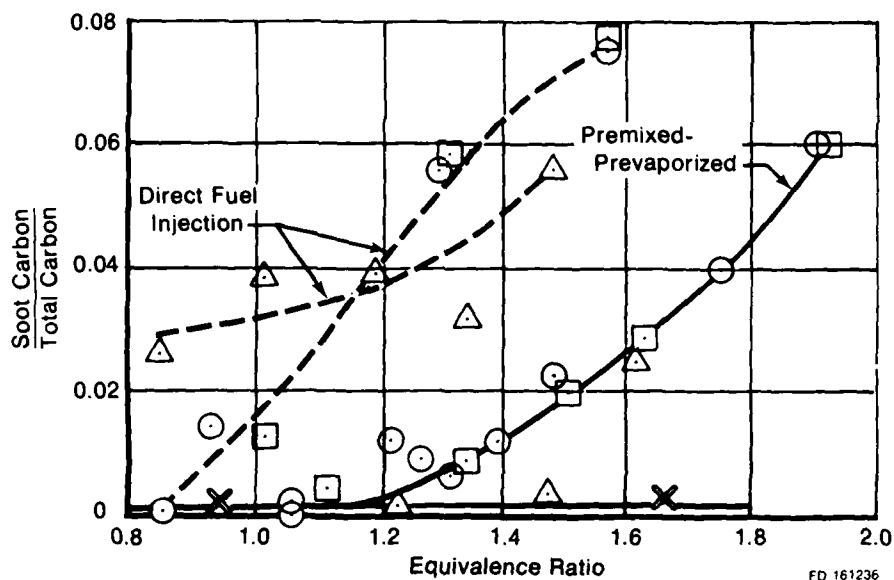


Figure 40. Effect of Fuel/Air Mixture Preparation on Soot Formation Using Kerosene Fuel

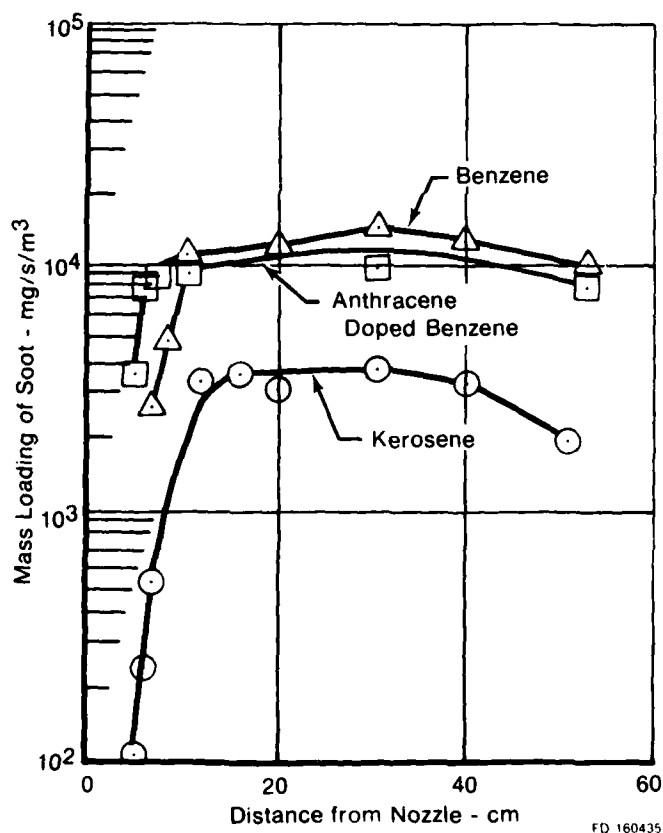


Figure 41. Influence of the Type of Fuel on Axial Profile of Soot Mass Loading — Fuel Equivalence Ratio = 1.0, Air Atomizing Pressure = 184 kPa (12 psig), Cold Gas Velocity = 0.96 m/sec

Phenomenologically, the effect of fuel composition on smoke and soot formation has been investigated in numerous instances. Figure 42 (Reference 31) shows the variation in smoke emitted from a JT8D combustor operated at the SLTO condition. The fuels tested were primarily Jet A-based with various single- and double-ring aromatic additives, as well as some shale-based fuels. The figure shows a reasonable correlation of smoke number with hydrogen content of the fuel despite the wide range of fuels tested. The effect of combustor inlet temperature is evident in Figure 43 (Reference 44), which shows parametrically the variation in smoke number with hydrogen content for temperatures from 394°K (250°F) to 644°K (700°F). The effect of both inlet temperature and pressure on smoke number is shown in Figure 44 (Reference 45). These data were obtained from smoke measurements on a Phillips 2-in. combustor operated on JP-5-based fuels with various single- and double-ring aromatics added. From these data, it is evident that inlet temperature is the more dominant factor of the two. At a 1000°F inlet temperature, a pressure variation from 75 to 225 psia only slightly affects the smoke emission dependence on hydrogen content. However, at 500°F, the variation in smoke number with hydrogen content is greater than at 1000°F, and there appears to be a slightly higher dependence of smoke number on pressure.

The susceptibility of the J57, TF30, and F100 combustors to increased smoke and soot formation is dependent upon particular design features such as fuel nozzle, swirl strength, and on operating conditions of temperature, pressure and fuel-air ratio. Since the quality of fuel atomization affects the soot formation, the reduced sensitivity of the airblast nozzle to increased fuel viscosity should help to minimize smoke formation for low hydrogen content, high viscosity fuels. The air which effects atomization in the F100 airblast nozzle results in a more intimate and rapid fuel-air mixing which also helps to reduce smoke formation.

The tendency to form smoke is strongly dependent upon the fuel loading in the primary zone of the combustor. A relative measure of this loading is afforded by comparing the axial variation in the gross equivalence ratio along the burner centerline for each of these combustors. This gross equivalence ratio is not, of course, the actual "burning" equivalence ratio. However, the extent to which the gross equivalence ratio is above unity is a relative measure of the propensity for droplet or diffusive burning, which favors soot formation. In all of these combustors, the gross equivalence ratio is well above unity in the dome region. However, in the J57, the gross equivalence ratio drops below unity in approximately 3 in., whereas in both the TF30 and F100 it is not only higher, but remains above unity for 4.5 in.

However, the factors which determine the initial formation of soot in the primary zone are not necessarily the dominant factors controlling the soot concentration at the exit of the combustor. Additional oxygen and high temperatures in the secondary zone of the combustor can effect oxidation of a large fraction of the carbon particles formed in the primary zone (References 46 and 47). On this basis, the F100 and TF30 may have lower net smoke emissions than the J57. The combination of high temperature and high equivalence ratio in the primary zone and low temperatures in the secondary zone are likely to result in a greater tendency for the J57 to emit smoke than for the other combustors.

EFFECT OF FUEL PROPERTIES ON ENGINE DURABILITY

Coking Of Fuel Systems

The increased aromatic content of relaxed-specification fuels is expected to have an impact on the tendency to form coke deposits in gas turbine fuel systems. This tendency has been investigated by Lohmann (Reference 10) in reference to the Experimental Reference Broad Specification (ERBS) fuel.

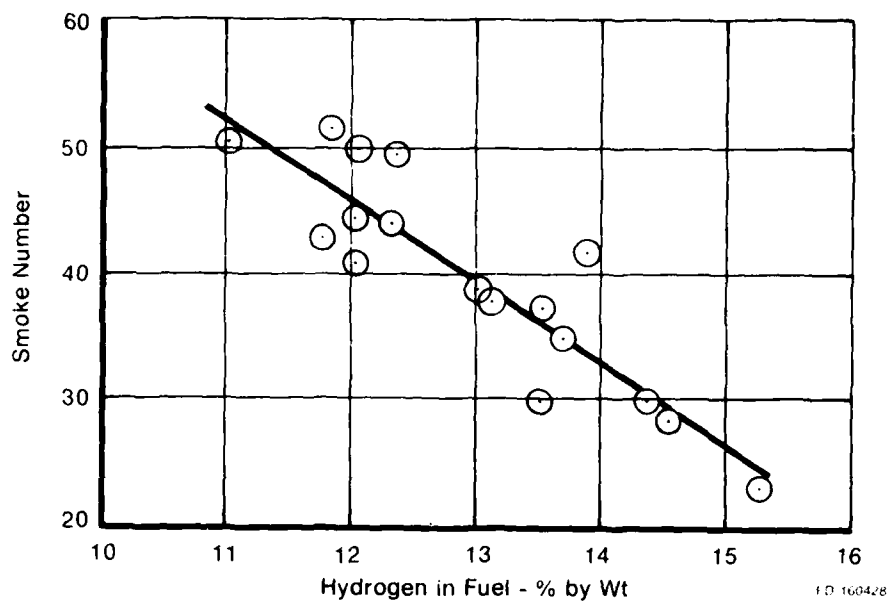


Figure 42. Effect of Hydrogen Content of Fuel on Smoke Number at Takeoff Condition

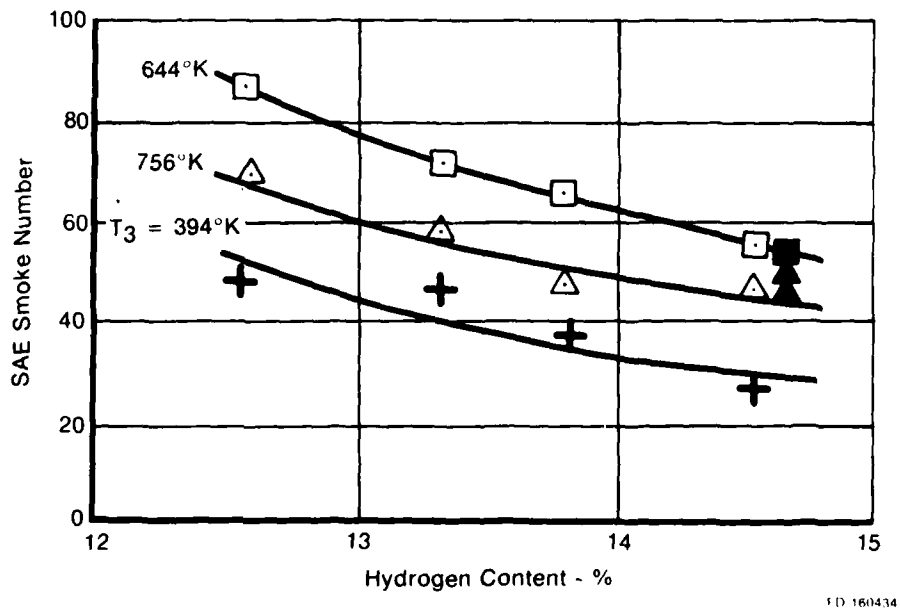


Figure 43. Smoke Emission Dependence on Hydrogen Content

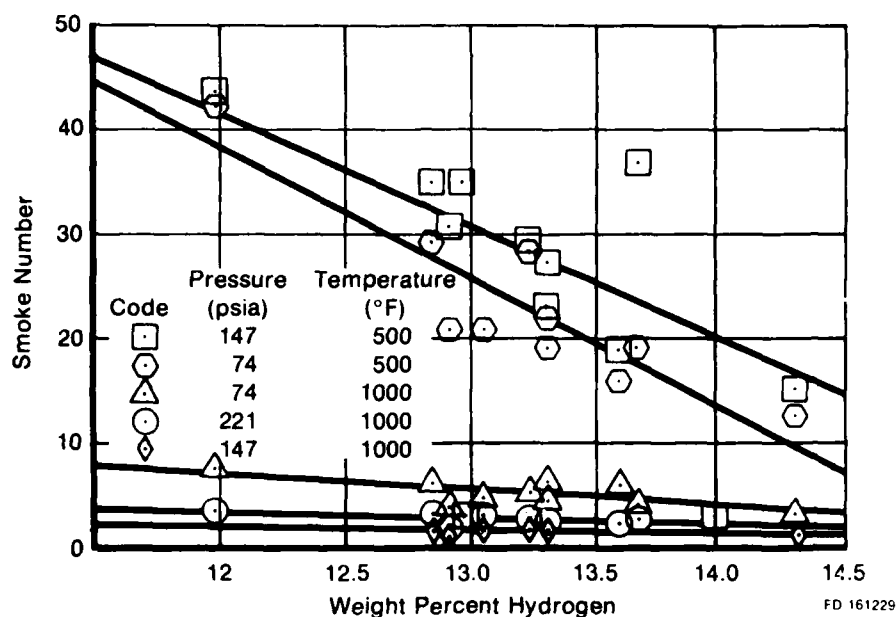


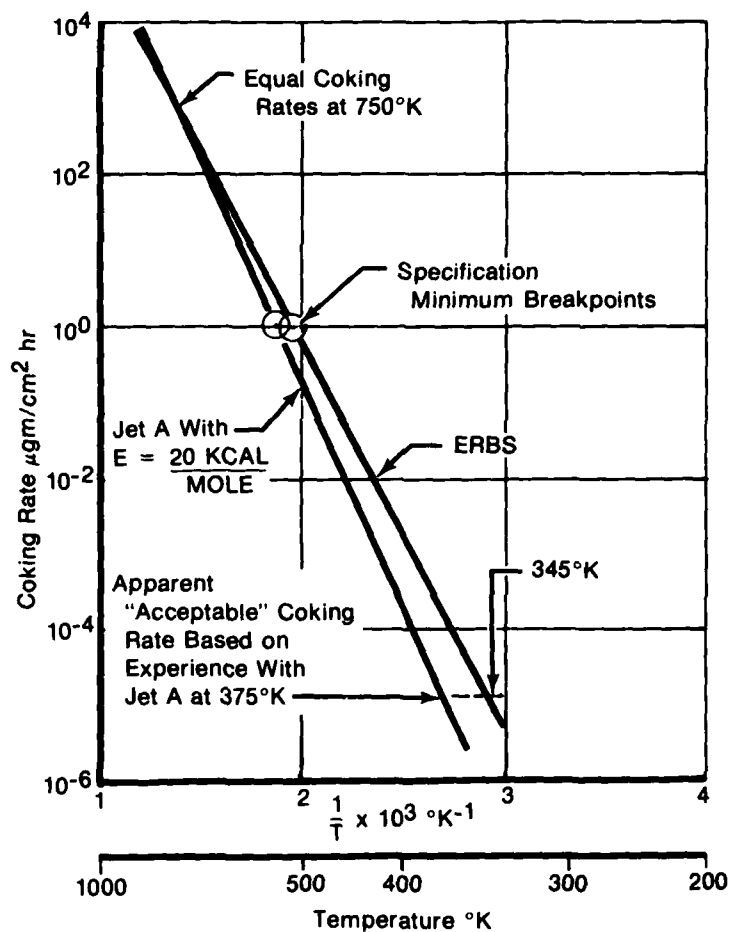
Figure 44. Effect of Pressure and Temperature of Exhaust Smoke

Table 14 shows the incremental changes in breakpoint temperature associated with the change in concentration between the nominal Jet A and ERBS compositions. The increase in the aromatic content itself is shown to be a major contributor to the reduction in thermal stability. The naphthalenes contribute less to the change in breakpoint temperature, but because naphthalene concentrations normally vary with aromatic content, the effects of both of these components should be considered in combination. The presence of olefins in even low concentrations has been shown to have a significant adverse effect on thermal stability, and the relatively small differences in the assumed concentration of this constituent in the nominal compositions of Jet A and ERBS is shown to have a substantial effect on the breakpoint temperature computed from this correlation.

TABLE 14. INCREMENTAL CHANGE IN BREAK-POINT TEMPERATURE FOR VARIOUS CHANGES IN FUEL COMPOSITION

Composition Change Relative to Jet A of Table 4-5	Breakpoint Temperature Increment Relative to Jet A at 500°F (°K)
Increase Aromatics to ERBS Level	9.9
Increase Naphthalene to ERBS Level	4.2
Increase Olefins to ERBS Level	9.4
Increase Sulphur to 0.3 percent	11.2
Increase Nitrogen to 1.0 percent	7.8

To estimate deposit formation rate as a function of temperature, it is necessary to assume that the formation rate exhibits Arrhenius-type dependence on temperature, and that the activation energy is known. Data on jet fuel generally indicate an overall mean activation energy for deposit formation in the range of 10-20 kcal per mole, with values of 15-20 kcal per mole more likely. Assuming a value of the activation energy of 20 kcal/mole, the temperature dependence of the coking rate from Jet A fuel has been constructed on Figure 45 using the ERBS fuel breakpoint temperature. A similar assumption regarding the activation energy for ERBS fuel could be made, but a better estimate of the coking rate is afforded by making use of the observation that in the temperature range of 700 to 800°K, many fuels of lower thermal stability exhibit the same coking rate as Jet A. Proceeding on this basis, and using the above-established coking rate at the breakpoint temperature of ERBS, a straight line is constructed on Figure 45 defining the temperature dependence of the coking rate for ERBS. The reduced slope of this characteristic, relative to that of Jet A, implies a lower activation energy and a weaker temperature dependence.



FD 161234

Figure 45. Predicted Coking Rates of Jet A and ERBS Fuel

Based on experience with Jet A fuel, coke formation in fuel injectors, supports and manifolds is minimal or nonexistent when the wall temperatures of these components are maintained at temperatures below 375°K. In the context of Figure 45, this implies the existence of an "acceptable" coke formation rate of about 10^{-5} $\mu\text{gm}/\text{cm}^2 \text{ hr}$. To achieve this "acceptable" level with ERBS fuel, it is necessary to reduce the maximum fuel passage temperatures to 345°K. This reduction in allowable surface temperature is about 36% greater than the difference in breakpoint temperatures and is a consequence of the reduced temperature sensitivity of coke formation of ERBS relative to Jet A.

Based on these observations, the following conclusions and design criteria are derived relative to the thermal stability of relaxed-specification fuels:

- Coke formation in inactive fuel systems or fuel vaporizers may be sensitive to the thermal stability characteristics of the fuel, but the extreme thermal environment may be the dominant mechanism in these situations.
- In active fuel systems, the coking rate has a strong temperature dependence. The reduced thermal stability of ERBS fuel will require a reduction in surface temperatures in the fuel system components of about 30°K to achieve the level of coking protection currently obtained with Jet A fuel.
- Correlation of data on the breakpoint temperature of fuel samples indicates a strong dependence on fuel composition including both major constituents and those normally present in only trace quantities. For the particular fuel samples analyzed, the aromatic, olefin, and sulphur contents were dominant, while the influence of the nitrogen content was not as severe as anticipated.

As discussed in Section II, the proposed changes in JP-4 and JP-8 fuel specifications are expected to have a negligible effect on the thermal stability of these fuels. Therefore, these relaxed-specification fuels should not have an adverse effect on coking in gas turbine fuel systems.

Combustor Liner

The increase in aromatic content associated with broadened-specification fuels can have a substantial impact on the radiant heat transfer to the combustor liner because of the increased concentrations of highly luminous carbon particulates in the combustion gases. This phenomena is more significant in the primary combustion zone where the carbon concentrations, local fuel-air ratios, and gas temperatures are the highest and have an obvious interrelation with smoke formation and control. This interrelation is evident from measurements of flame radiation and smoke emissions. Figures 46 and 47 (Reference 48) compare radiation and smoke emissions from a T-63 combustor as a function of hydrogen-to-carbon atom ratio of the fuel. With this correlating parameter, radiation and smoke concentrations exhibit nearly an identical response.

Radiant heat loads to the combustor liner are more commonly correlated with fuel hydrogen content. Figure 48 illustrates the increase in radiant heat load with decreasing fuel hydrogen content (decreasing aromaticity) for two full size combustors, the J57 and J79, and two sub-scale experimental combustors (Reference 49). This figure also shows that the sensitivity or thermal radiation to variations in hydrogen content is dependent on combustor type. The factors which influence the sensitivity of thermal radiation to variations in hydrogen content for a given combustor are essentially the same factors affecting smoke and soot formation: fuel nozzle atomization quality, swirl strength, fuel-to-air ratio, inlet temperature and pressure, and primary-zone equivalence ratio. Based on these factors, the impact of low-hydrogen fuels on thermal radiation is expected to be greatest for the J57 combustor and decrease with the TF30 and F100 combustors.

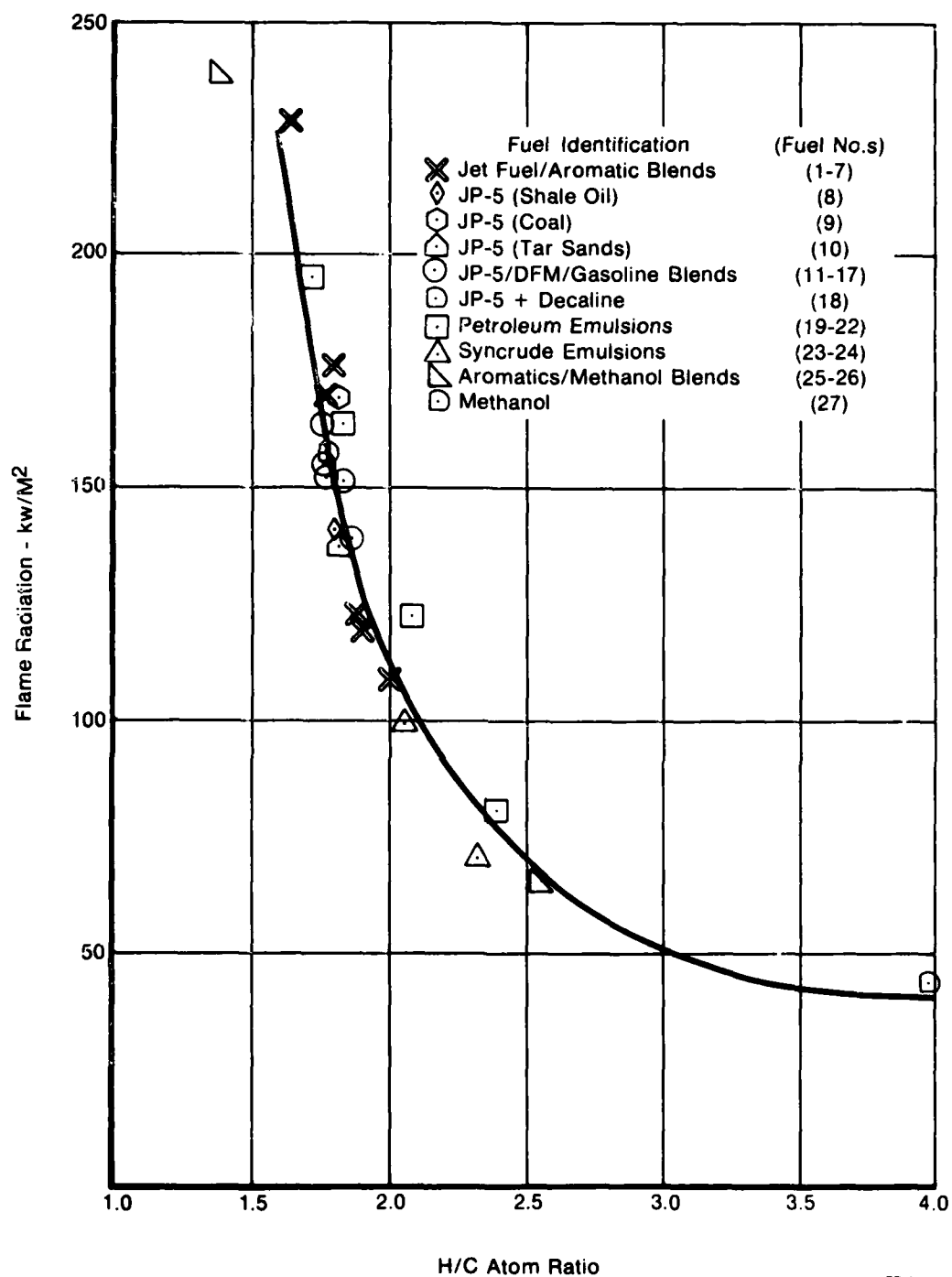
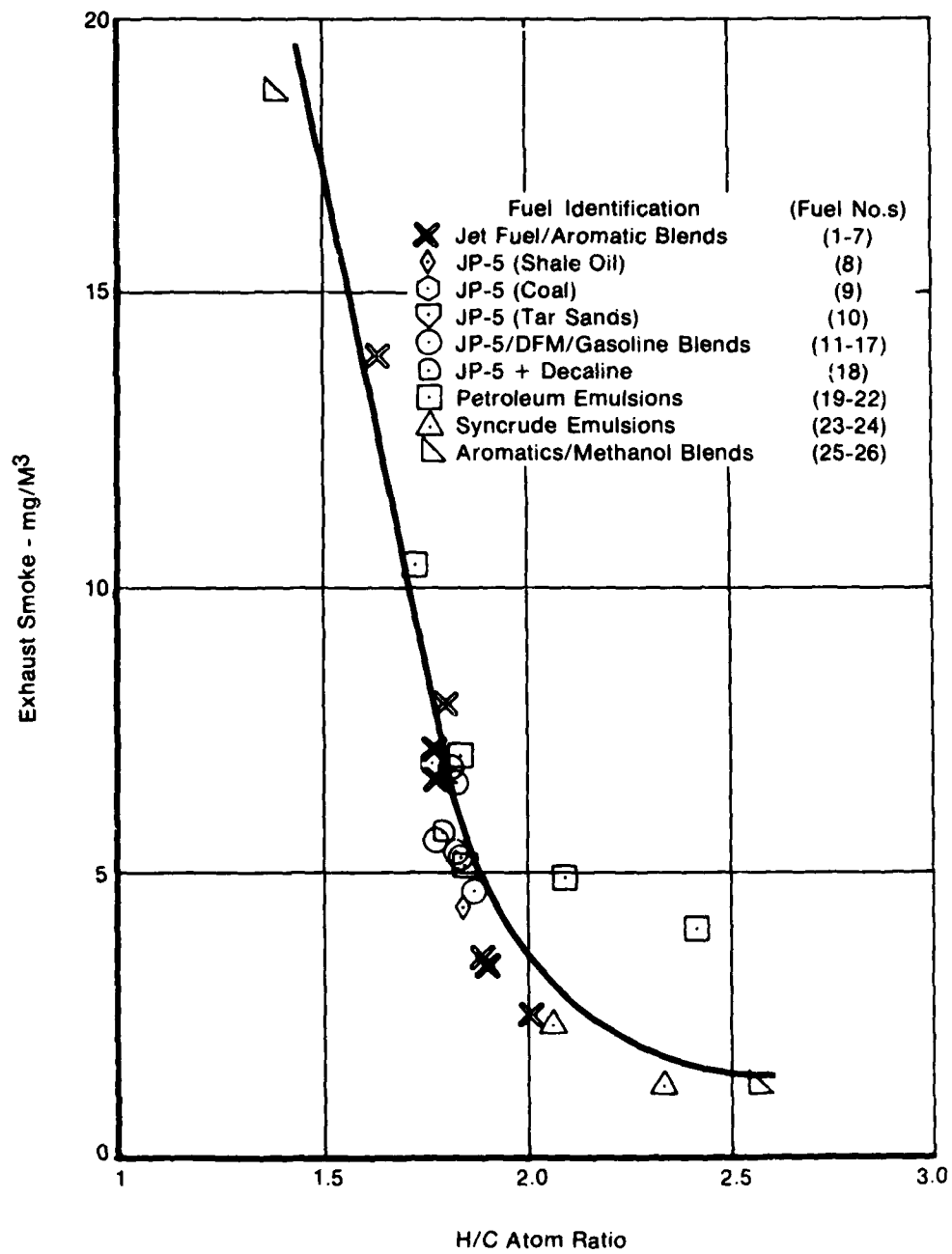
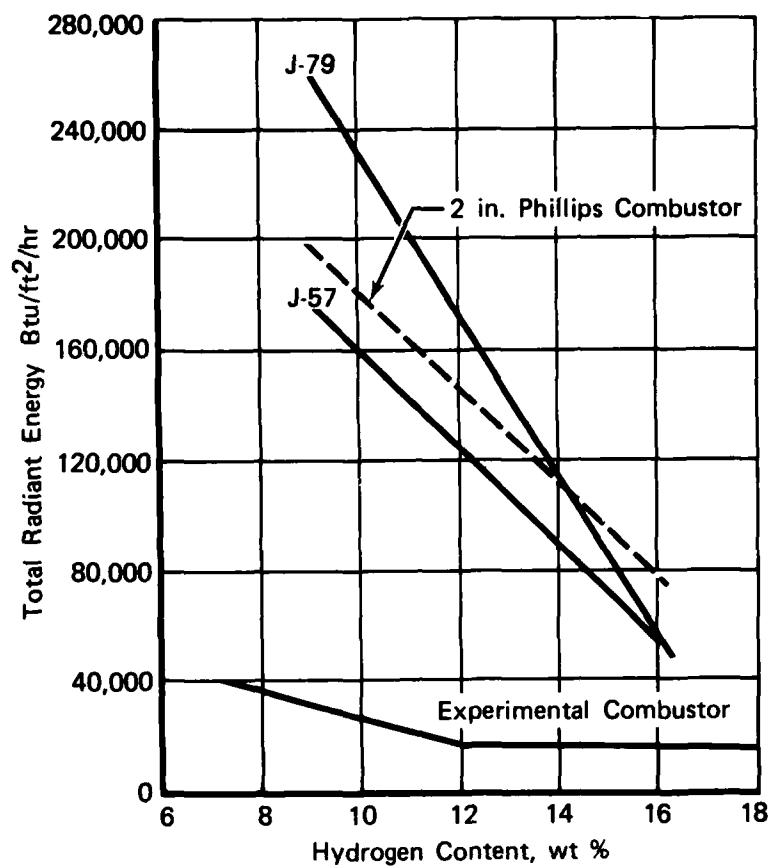


Figure 46. Effect of Hydrogen to Carbon Ratio on Flame Radiation



FD 161227

Figure 47. Effect of Hydrogen to Carbon Ratio on Exhaust Smoke



FD 173418

Figure 48. Variation in Flame Radiation With Hydrogen Content for Several Combustors

The sensitivity of liner temperatures to variations in fuel hydrogen content is also dependent on combustor type. Several investigators have conducted tests on instrumented, louver cooled gas turbine combustors using fuels of varying hydrogen content (References 31, 50 and 51). Results of these tests showed that liner temperatures correlate well with hydrogen content, and increases in liner temperatures ranging from 25 to 100°F were found at high-power conditions for a decrease in fuel hydrogen content of approximately 1%. This range in measured temperature rise is due to a number of factors, including differences in operating conditions, louver design, and thermocouple locations. Figure 49 illustrates the effect of fuel hydrogen content on peak liner temperatures in a JT8D combustor (Reference 31). As shown, the sensitivity of peak liner temperatures to hydrogen content is greater at a lower combustor inlet temperature (cruise) than at the maximum inlet temperature (SLTO). Blazowski (Reference 52) has correlated liner temperature rise with hydrogen content for a number of mature combustors using a liner temperature parameter (LTP), as shown in Figure 50. The LTP represents the average liner temperature rise relative to a base fuel (JP-4) normalized by the difference between average liner temperature and combustor inlet temperature for the base fuel. All combustors represented in Figure 50 are of can annular design and have pressure-atomizing fuel nozzles. Data obtained using several advanced combustors (References 29 and 53) have indicated somewhat less sensitivity of liner temperatures to hydrogen content. Although the Blazowski correlation is useful in identifying trends in average liner temperature with decreasing hydrogen content, several points concerning this correlation should be made. First, unless a means is available for relating average liner temperature to peak liner temperatures or peak temperature gradients, the correlation is only valid for predicting trends in combustor durability. Also, while the data scatter trends to coalesce at high values of hydrogen content, at lower values (below 13%) the data scatter increases greatly until at a hydrogen content of 11%, the data scatter band is 50% of the temperature rise parameter. The fact that the data scatter increases with decreasing hydrogen content indicates that the various combustors exhibit different sensitivities to hydrogen content.

Increases in peak liner temperatures resulting from the use of relaxed-specification fuels may decrease combustor durability primarily through two types of failure: oxidation/erosion and low-cycle fatigue (LCF) cracking. Oxidation/erosion occurs when local liner temperatures are of sufficient magnitude to cause a gradual chemical reaction in which the surface of the liner material oxidizes, starts flaking, and is removed from the surface by scrubbing of the gas stream. LCF occurs when axial and circumferential temperature gradients in the liner result in induced material strains which may exceed the elastic limits of the material at points of maximum stress and/or temperature. With each cycle in engine conditions, additional strain is incurred until failure occurs. There are many failure modes for the liner, and each exhibits a dependence on the total number of cycles and the maximum temperature of the liner in the cycle. This behavior is shown in Figure 51 (Reference 54) for a typical commercial aircraft engine. The assessment of combustor liner life in a military engine may be more difficult since the combustor may be replaced before failure occurs.

Turbine Airfoils

The use of relaxed-specification fuels may also have an impact on the durability of turbine airfoils. Airfoil durability is dependent upon metal temperature patterns which can be influenced by changes in combustor operation characteristics due to relaxed-specification fuels.

As discussed previously, broadened-specification fuels are expected to increase emissions of carbon particulates. Heavier carbon particles, usually considered to be those particles having an equivalent spherical diameter of 20-25 microns or greater, can depart from the gas path streamlines and impinge upon airfoil surfaces. The impact can erode turbine surface coatings and cause a buildup of carbon material on the pressure sides of turbine airfoils. This causes airfoil surface roughness to increase, and thus increases the convective heat transfer to the airfoils.

There is little data currently available in the literature which can be used to establish a relationship between particle size distribution and fuel properties. The relatively small changes in JP-4 and JP-8 fuel properties considered in this study, however, are not expected to increase the number of large carbon particles by a sufficient amount to decrease turbine durability.

A number of investigators (References 29, 30, and 53) have studied the effect of relaxed-specification fuels on burner exit pattern factor and radial temperature profiles. Results from these studies have demonstrated these parameters are relatively insensitive to fuel type. Figure 52 illustrates the results of tests performed on a J79 combustor with fuels ranging in hydrogen content from 12 to 14.5% (Reference 30). As shown, there is essentially no effect of fuel type on pattern factor at all power conditions. In addition, a recent USAF-sponsored program (Reference 21) measured the burner exit pattern factor of an F100 combustor using both JP-4 and JP-8 fuels and found no discernible change. The proposed fuel property changes for JP-4 and JP-8 fuels, therefore, are not expected to decrease turbine durability in the TF30, J57, and F100 engines by virtue of a change in turbine airfoil temperature patterns. It should be mentioned, however, that a sufficient change in fuel properties could necessitate increasing the proportion of burner airflow used for cooling of the combustor liner. If this occurs, changes in burner exit pattern factors and radial temperature profiles would result, and turbine durability would decrease. Also, the use of broadened-specification fuels having reduced thermal stability could lead to a greater propensity for fuel nozzle plugging, which would alter spray patterns and increase the pattern factor.

The increase in flame luminosity associated with the use of relaxed-specification fuels is expected to increase the radiant energy incident upon turbine inlet guide vanes. The magnitude of this increase is expected to be small, however, for two reasons: First, due to high local velocities at inlet guide vane surfaces (local Mach numbers approach unity), convective heat loads to inlet vanes are high and account for the majority of the total inlet vane heat load. Second, the viewing angle of inlet guide vanes to the combustor primary zone (where flame luminosity and radiative temperatures are highest) is relatively small, especially for can-annular combustor designs. With regard to viewing angle, the increase in radiant energy incident upon inlet guide vanes due to increased fuel aromaticity should be greater for the F100 combustor than for the J57 and TF30. In Section IV of this report, radiant and convective heat loads to F100 and J57 inlet vanes are estimated and compared to determine the effect of the proposed changes in JP-4 and JP-8 fuel properties.

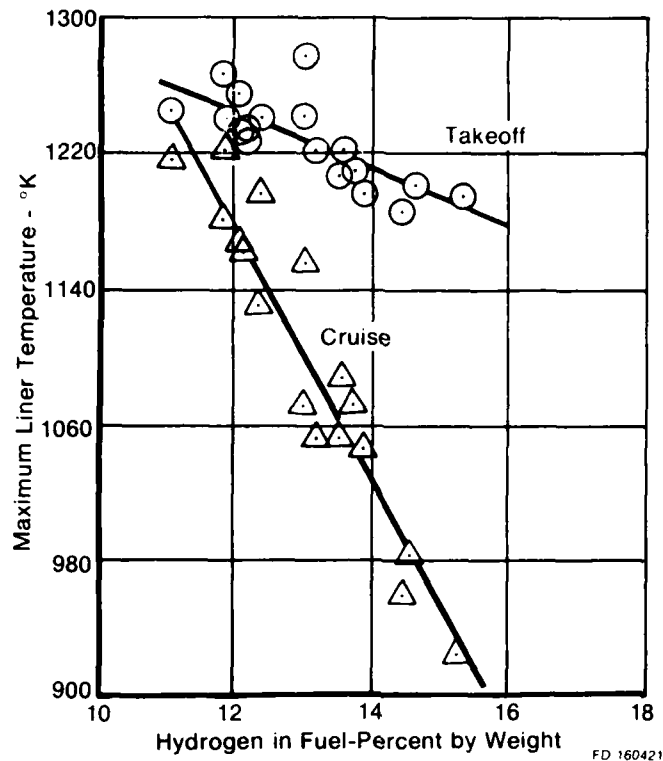


Figure 49. Effect of Hydrogen Content of Fuel on Maximum Liner Temperatures at Cruise and Takeoff Conditions

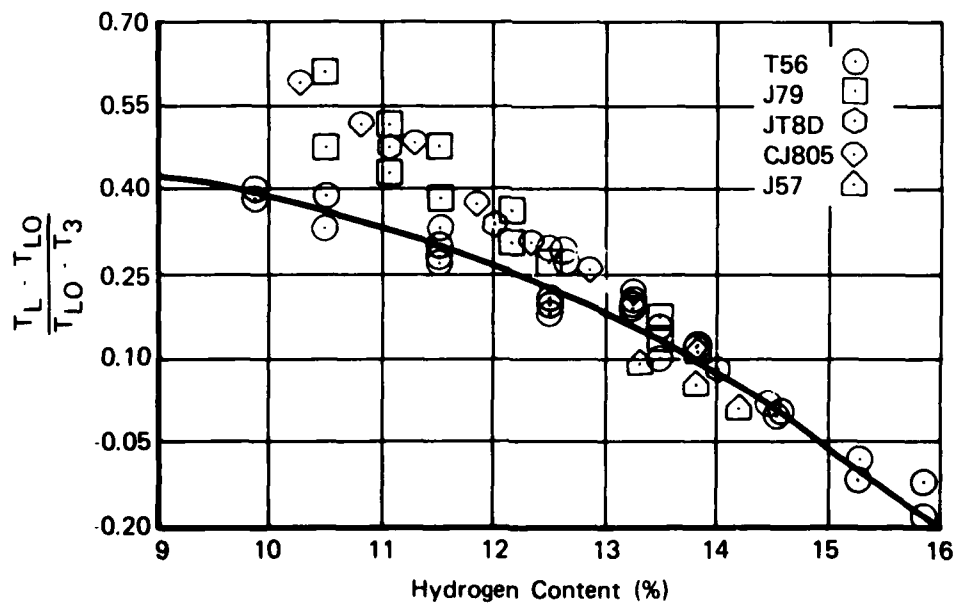
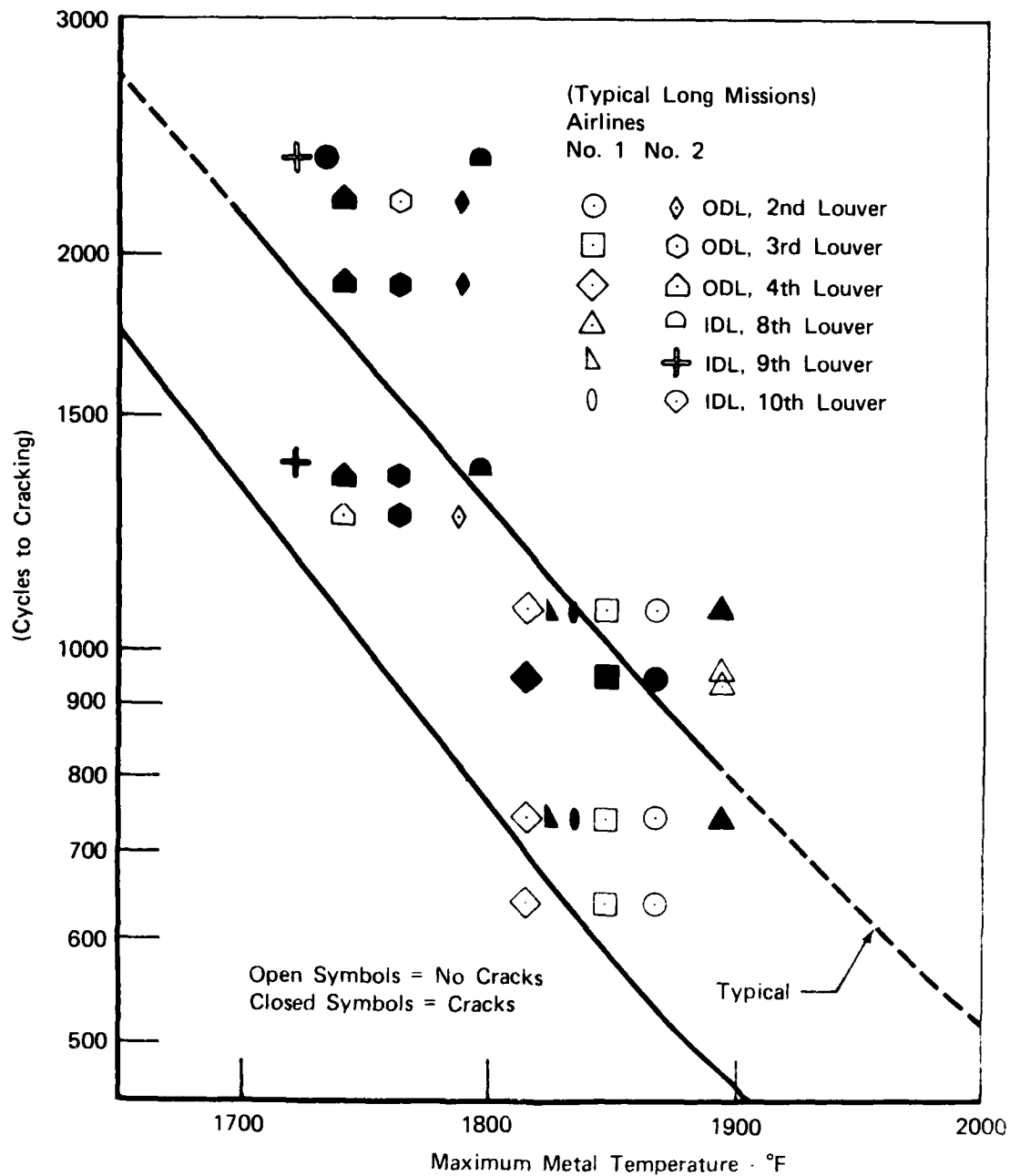


Figure 50. Liner Temperature Correlation for Different Combustor Types



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Figure 51. Correlation of Annular Burner Creep - LCF Model

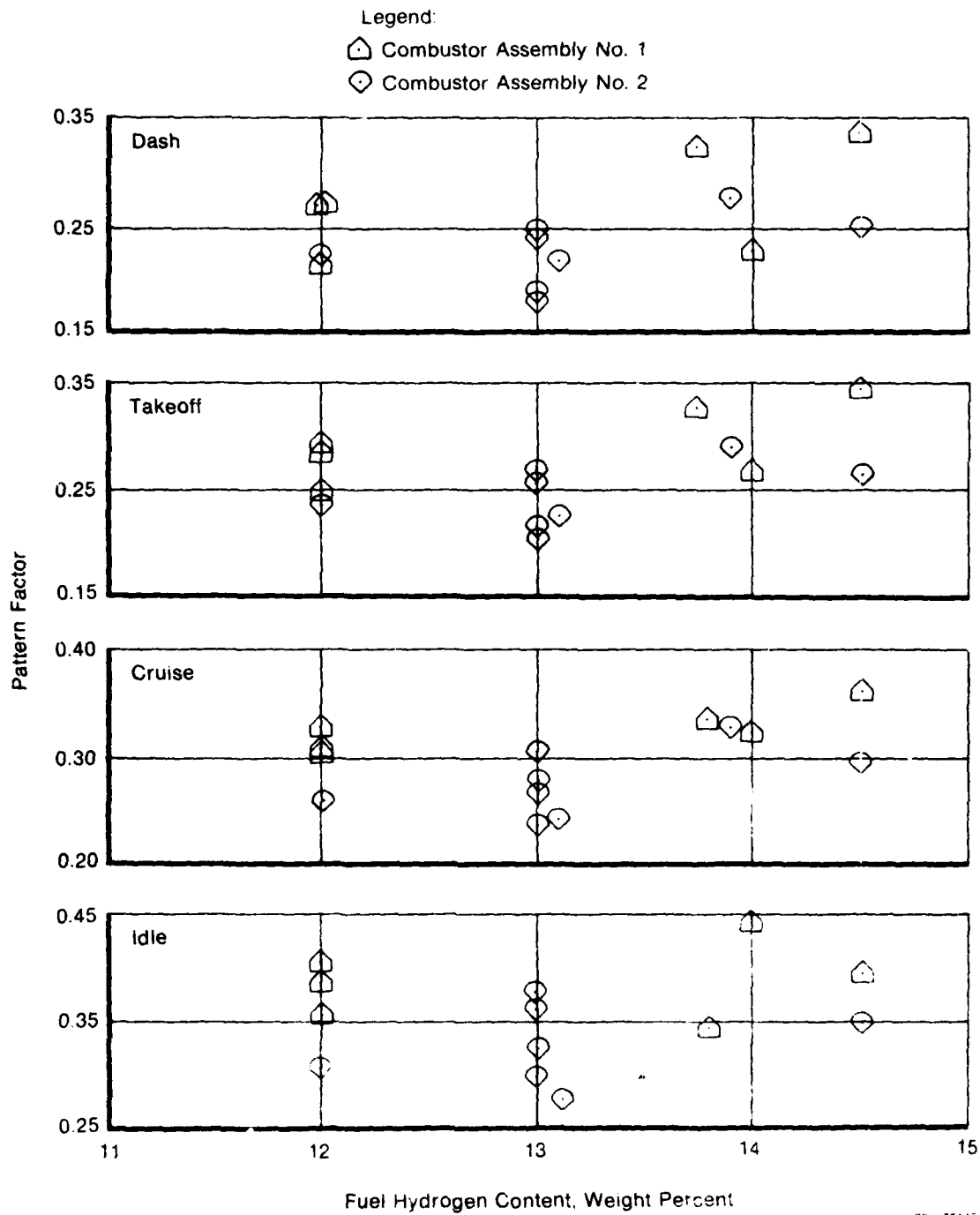


Figure 52. Effect of Fuel Hydrogen Content on Pattern Factor

SECTION IV

THERMAL ANALYSIS OF F100 AND MATURE (J57) ENGINES

Thermal analyses were conducted for both the F100(3) and J57-59W engines to determine the effect of the proposed relaxations of fuel specifications on combustor liner and turbine inlet guide vane temperatures. The procedures used to determine this effect, and the resulting changes in metal temperatures, are presented in the following sections.

COMBUSTOR THERMAL ANALYSES

Thermal Model

The thermal model used in this study is part of a comprehensive liner design computer program developed by Pratt & Whitney Aircraft (P&WA) to predict pressure losses, airflow distribution, hot gas temperatures, and metal wall temperatures for gas turbine combustors. The program can be used for combustor design purposes, or to study the effects of modifications or changes to burner geometry and operating conditions on existing combustors. The computer program is functionally divided into two parts: a flow model and a thermal model. The flow model calculates the pressure distribution around and within the combustor and the proportional airflow rates for the burner front end, cooling holes, and dilution holes. The thermal model then uses this information to determine hot gas parameters along the burner centerline, and to calculate metal temperatures along each combustor cooling panel.

The program uses three coordinate systems to perform the necessary computations. Combustor geometry, either annular or can-annular, is input in terms of the engine centerline coordinate system. The burner centerline coordinate system is used to define the flowpath, and all hot gas parameters are calculated as a function of distance along the burner centerline. Finally, a cooling panel coordinate system is used to obtain liner metal temperatures as a function of distance along each panel.

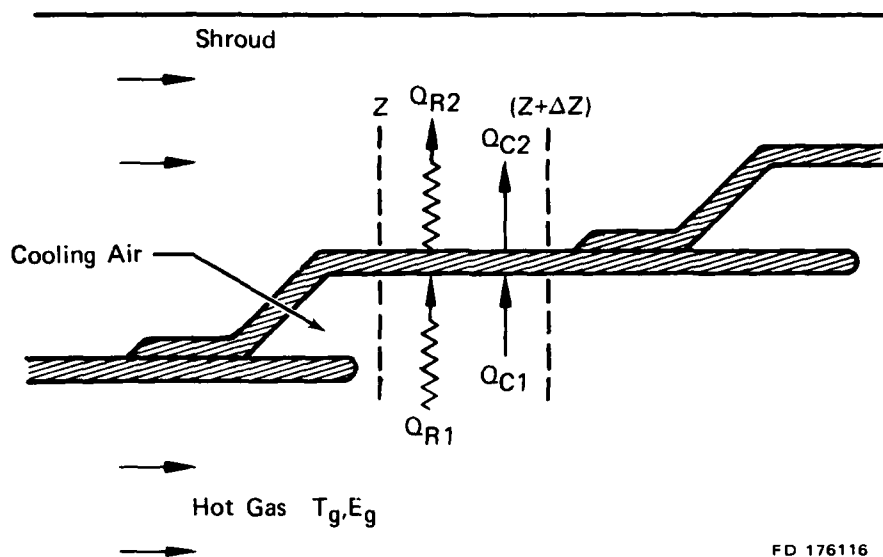
Metal temperatures are calculated at several locations along each panel by performing a heat balance on an incremental area corresponding to each location. Referring to Figure 53, a sketch of a typical louver-cooled combustor liner section, the following components are considered in each heat balance:

- Radiative heat transfer from the hot gases to the louver wall, Q_{R1}
- Convective heat transfer from the hot gases to the louver wall, Q_{C1}
- Radiative heat transfer from the louver wall to the shroud wall, Q_{R2}
- Convective heat transfer to the shroud air flow, Q_{C2}

Equating the heat addition over an incremental length to the heat loss yields

$$Q_{R1} + Q_{C1} = Q_{R2} + Q_{C2} \quad (24)$$

Since each of the heat flux terms of Equation 24 is dependent upon the louver wall temperature, an initial guess of the temperature must be made, and the heat balance equation is solved iteratively at each location.



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Figure 53. Thermal Heat Flux Loads on a Louver-Cooled Combustor Liner

The increase in aromaticity associated with relaxed specification fuels has the primary effect of increasing the radiant heat load from the hot gas to the louver walls. Although specified fuel properties cannot be input directly to the liner design program, the effect of fuel properties on Q_{R1} can be evaluated using a luminosity factor, a parameter which indicates the ratio of luminous to nonluminous flame radiation. The dependence of thermal heat loading on flame luminosity may be shown by examining the equations used to calculate the Q_{R1} heat flux component as follows:

$$Q_{R1} = \sigma_{sh} \left[\frac{1 + \epsilon_w}{2} \right] [\epsilon_g T_g^4 - \alpha_g T_w^4] \quad (25)$$

where

- σ_{sh} = Stefan-Boltzmann constant, $0.1713 \times 10^{-8} \text{ Btu/ft}^2\text{-hr-}^\circ\text{R}$
- ϵ_w = Emissivity of the liner wall
- α_g = Absorptivity of the hot gases
- ϵ_g = Emissivity of the hot gases
- T_g = Hot gas temperature
- T_w = Liner wall temperature.

Hot gas emissivity and absorptivity are found using the following equations

$$\epsilon_g = 1 - \text{EXP} \left\{ -18.26 \frac{PL}{T_g} \left[\frac{BL f/a}{T_g} \right]^n \right\} \quad (26)$$

$$\alpha_g = 1 - \text{EXP} \left\{ -18.26 \frac{PL}{T_w} \left[\frac{BL f/a}{T_w} \right]^n \right\} \quad (27)$$

where

P = Combustor pressure
 L = Luminosity factor
 BL = Beam length
 f/a = Fuel-air ratio
 T_g = Hot gas temperature
 T_w = Liner wall temperature.

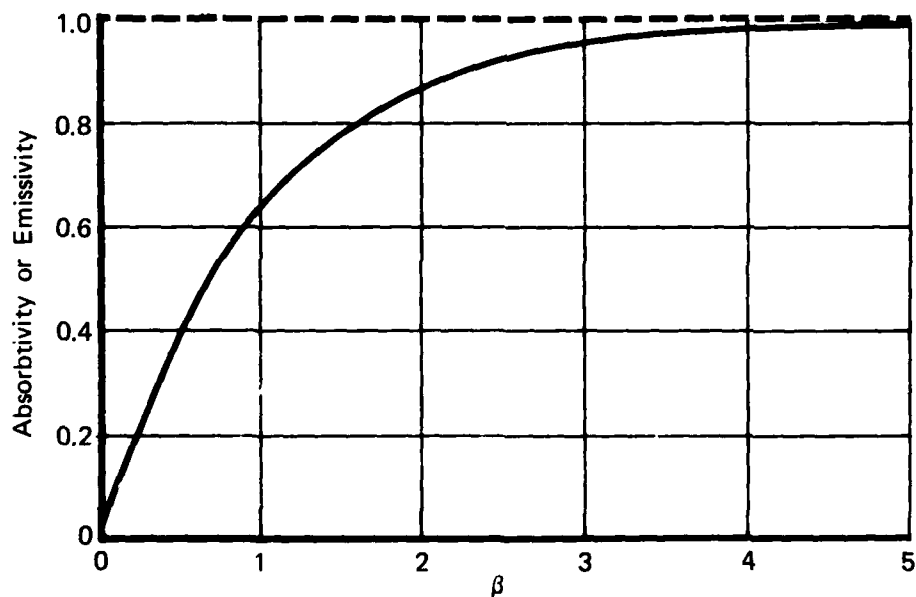
From Equations 26 and 27, it is evident that an increase in the luminosity factor results in an increase in hot gas emissivity, which acts to increase the radiant heat loading, and an increase in hot gas absorptivity, which acts to decrease the radiant heat loading. The change in the net heat transfer to the liner wall by virtue of radiation can be examined by defining a variable β such that

$$\beta = 18.26 \frac{PL}{T_i} \left(\frac{BL f/a}{T_i} \right)^{1/4} \quad (28)$$

where

T_i = Hot gas or liner wall temperature.

The variation of hot gas emissivity with β is then shown in Figure 54. As is shown, both absorptivity and emissivity asymptotically approach unity as β is increased; however, the sensitivity of either hot gas absorptivity or emissivity to changes in β decreases as the magnitude of β is increased. Therefore, since the flame temperature is always greater than the liner wall temperature at a given location, an increase in luminosity yields a greater increase in emissivity than absorptivity, and the net radiant heat load to liner walls is increased.



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Figure 54. Variation in Flame Emissivity With β

The liner design program was used to predict liner wall temperatures for both the F100(3) and J57-59W combustors at four engine operating conditions. These operating conditions are shown in Tables 15 and 16 for the F100 and J57 combustors, respectively. At each power level, liner temperatures and radiant heat loads were found for luminosity factors ranging from 1.0 to 1.6. A sample of the results of these computer runs is presented in Figures 55 through 62 for sea-level-take-off (SLTO) operating conditions. Individual louvers are identified as either ID or OD, and are numbered sequentially for each burner beginning with the first upstream louver as indicated in Figures 63 and 64. Referring to Figures 55 and 56, the peak louver temperatures for J57 louvers located in the primary zone exhibit a greater sensitivity to changes in luminosity than do downstream louvers. Radiant heat loads corresponding to these peak liner temperatures also exhibit similar behavior, as shown in Figures 57 and 58 for several ID and OD louvers. The sensitivity of peak louver temperatures and radiant heat loads to changes in luminosity for the F100 combustor, however, remains essentially constant regardless of louver location, as shown in Figures 59 through 62.

TABLE 15. F100(3) OPERATING CONDITIONS

Setting	Burner Airflow (lb _m /sec)	Inlet Pressure (psia)	Inlet Temperature (°F)	f/a (lb _m /lb _{ma})
Idle	32.65	63.01	428	0.0093
Cruise	71.77	189.40	960	0.0215
SLTO	135.51	366.29	1,027	0.0210
Dash	100.9	271.61	1,149	0.0193

TABLE 16. J57-59W OPERATING CONDITIONS

Setting	Burner Airflow (lb _m /sec)	Inlet Pressure (psia)	Inlet Temperature (°F)	f/a (lb _{ma} /lb _{mi})
Idle	48.0	38.2	194	0.0050
Cruise 1*	53.12	49.6	449	0.0102
Cruise 2*	77.4	74.9	535	0.0118
SLTO	174.0	180.0	680	0.0139

*Engine operating conditions with (Cruise 2) and without (Cruise 1) payload.

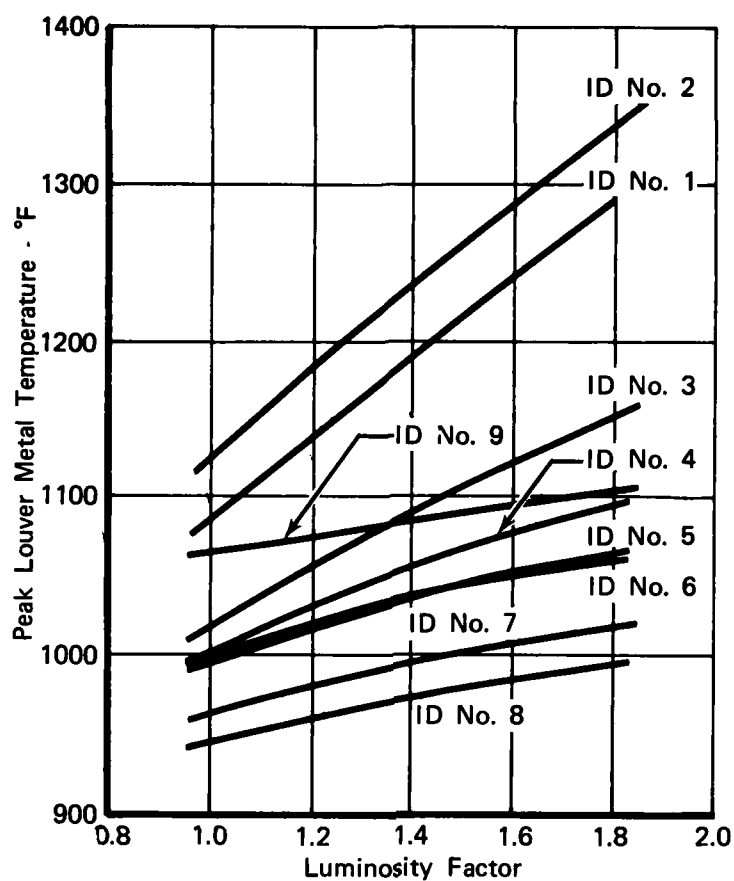
Computational Procedure

After using the liner design program to determine variations in liner temperatures and heat loads with luminosity, a three-step procedure was employed to predict changes in liner temperatures resulting from the relaxed specification fuels. First, changes in fuel hydrogen content corresponding to the proposed changes in fuel specifications were found for each fuel as discussed and presented previously in Section 2. Next, the changes in fuel hydrogen content were used to assess an increase in the radiant heat load to the combustor liners. This was accomplished using Figure 65. This data was obtained from radiation measurements on a J57 combustor operated at a scaled cruise condition (inlet pressure of 69.1 psia) using fuels ranging in hydrogen content from approximately 9 to 16%. The relationship between hydrogen content and radiant energy, shown Figure 65, found in these tests was

$$Q_{R1} = 334,830 - 17,333 H \text{ (B/hr-ft}^2\text{)} \quad (29)$$

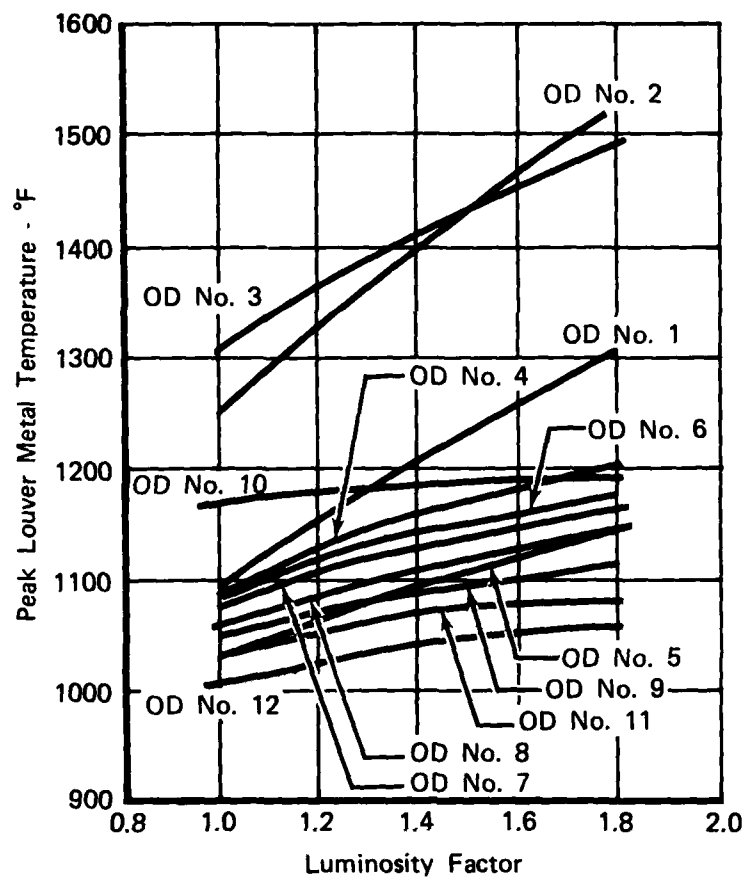
As applied to the procedure used in this study, only the sensitivity of Q_{R1} to hydrogen content was used, and thus the requisite equation is

$$\Delta Q_{R1} = -17,333 \Delta H \text{ (B/hr-ft}^2\text{)} \quad (30)$$



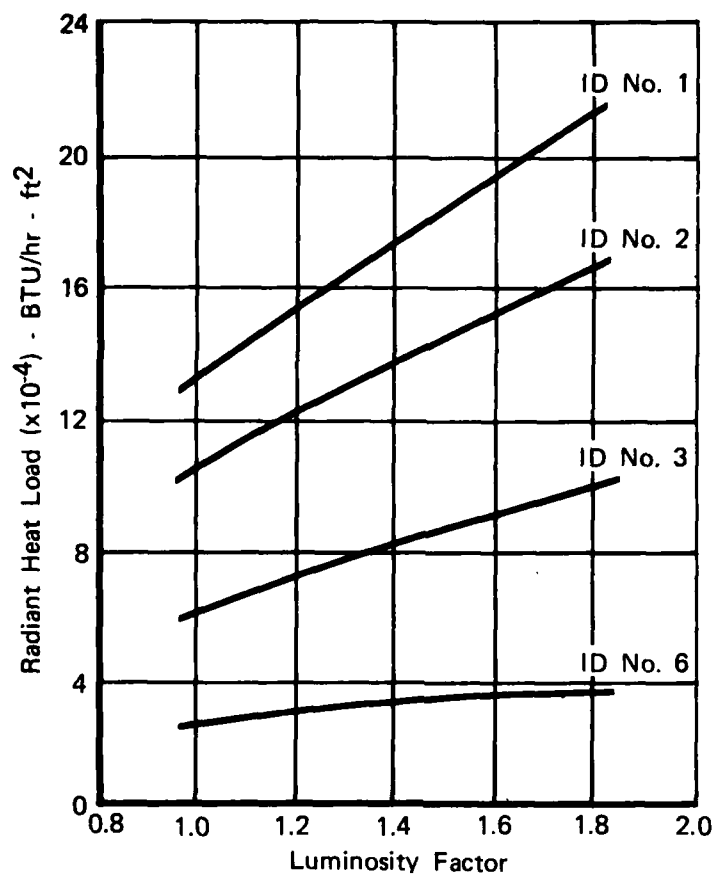
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Figure 55. ID J57 Louver Temperatures vs Luminosity Factor at SLTO Conditions



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Figure 56. OD J57 Louver Temperatures vs Luminosity Factor at SLTO Conditions



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Figure 57. Radiant Heat Loads Corresponding to Peak ID J57 Louver Temperatures at SLTO Conditions

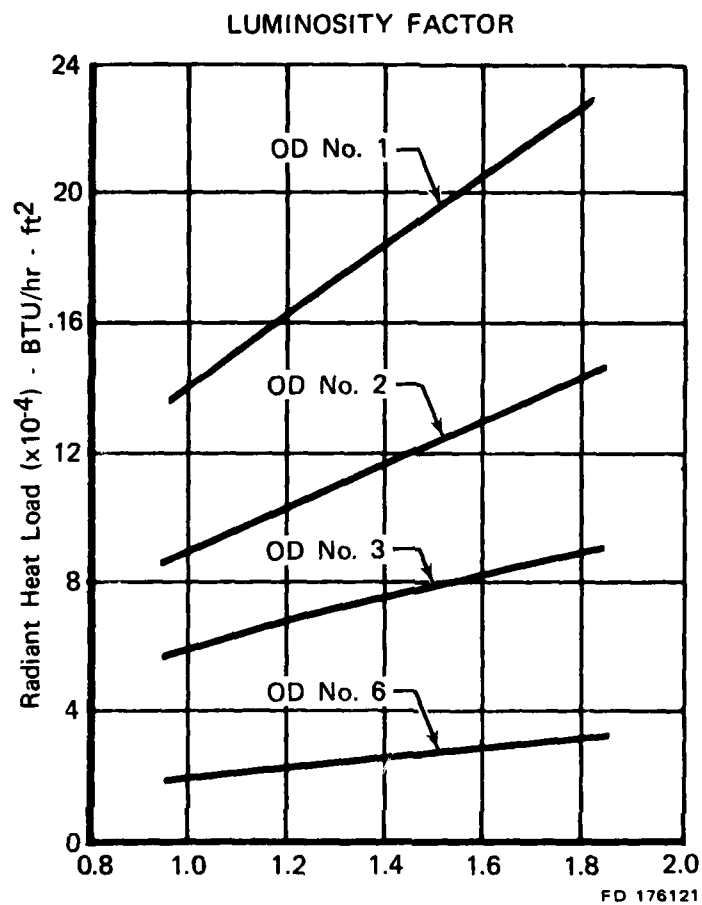
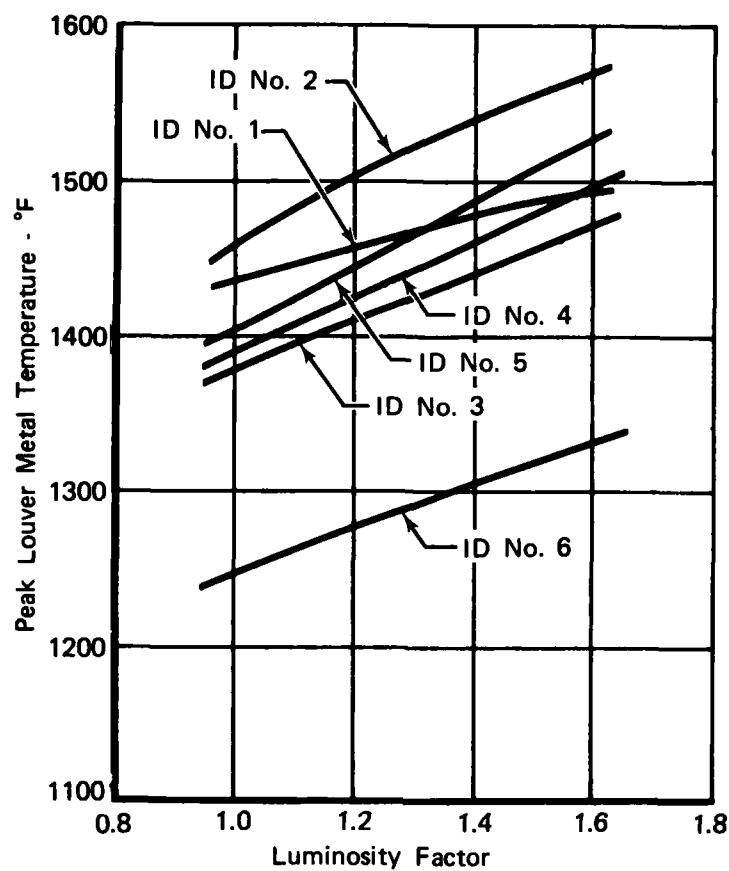
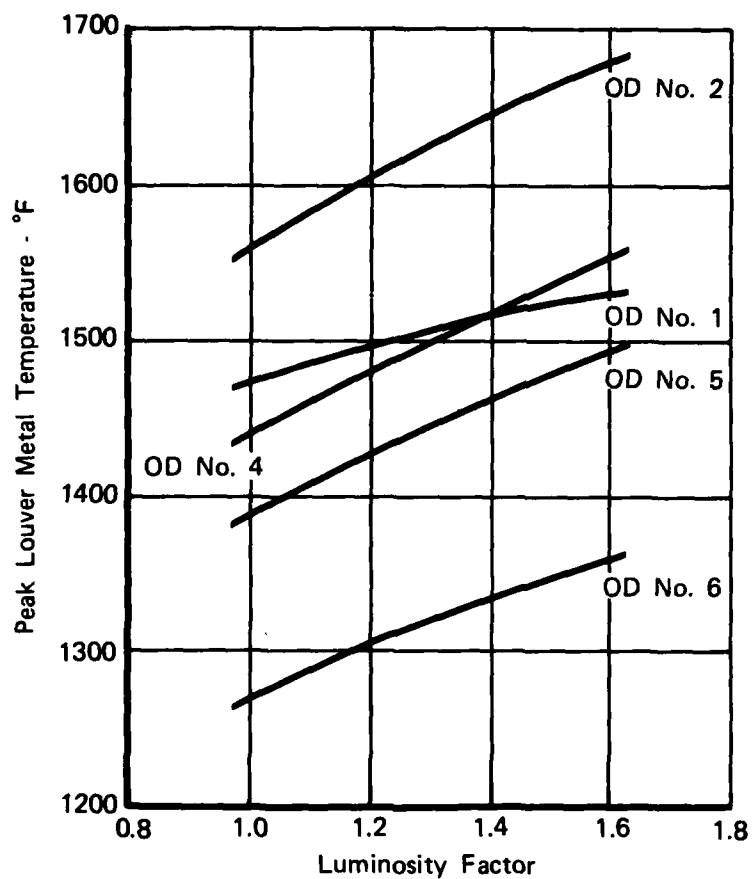


Figure 58. Radiant Heat Loads Corresponding to Peak OD J57 Lower Temperatures at SLTO Conditions



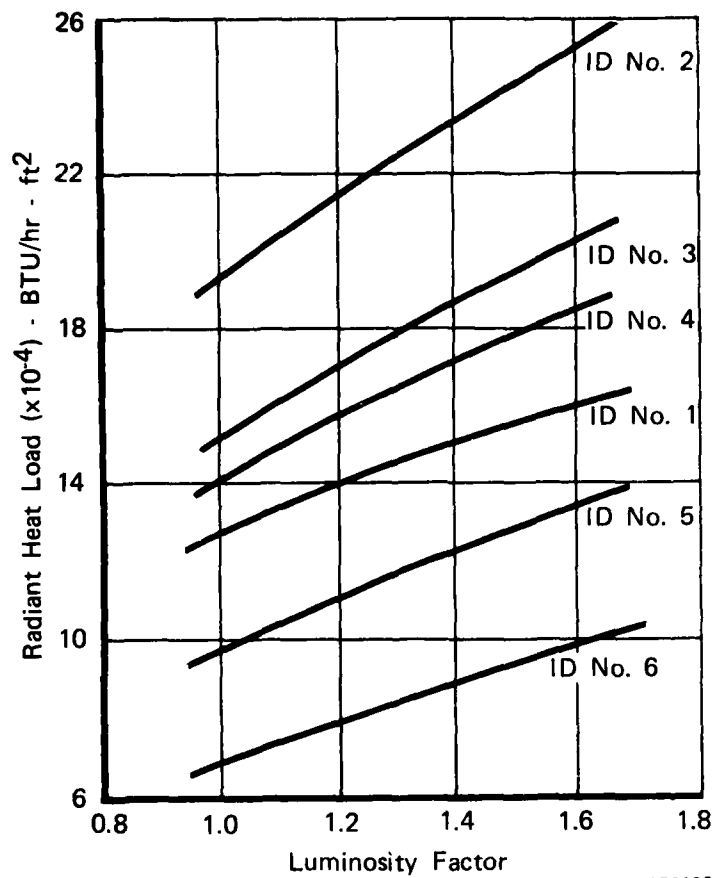
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Figure 59. ID F100 Louver Temperatures vs Luminosity Factor at SLTO Conditions



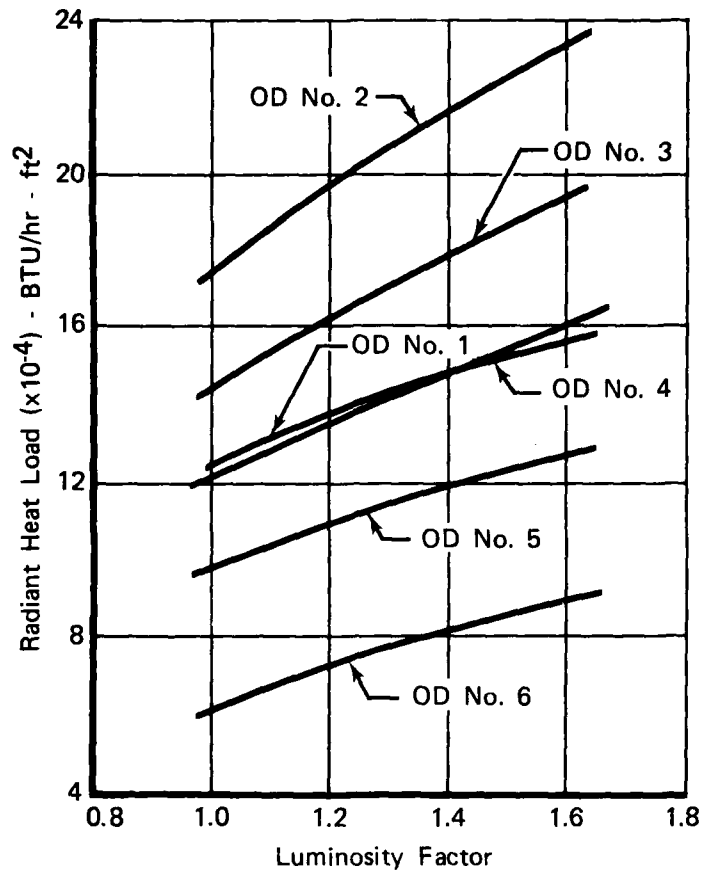
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Figure 60. OD F100 Louver Temperatures vs Luminosity Factor at SLTO Conditions



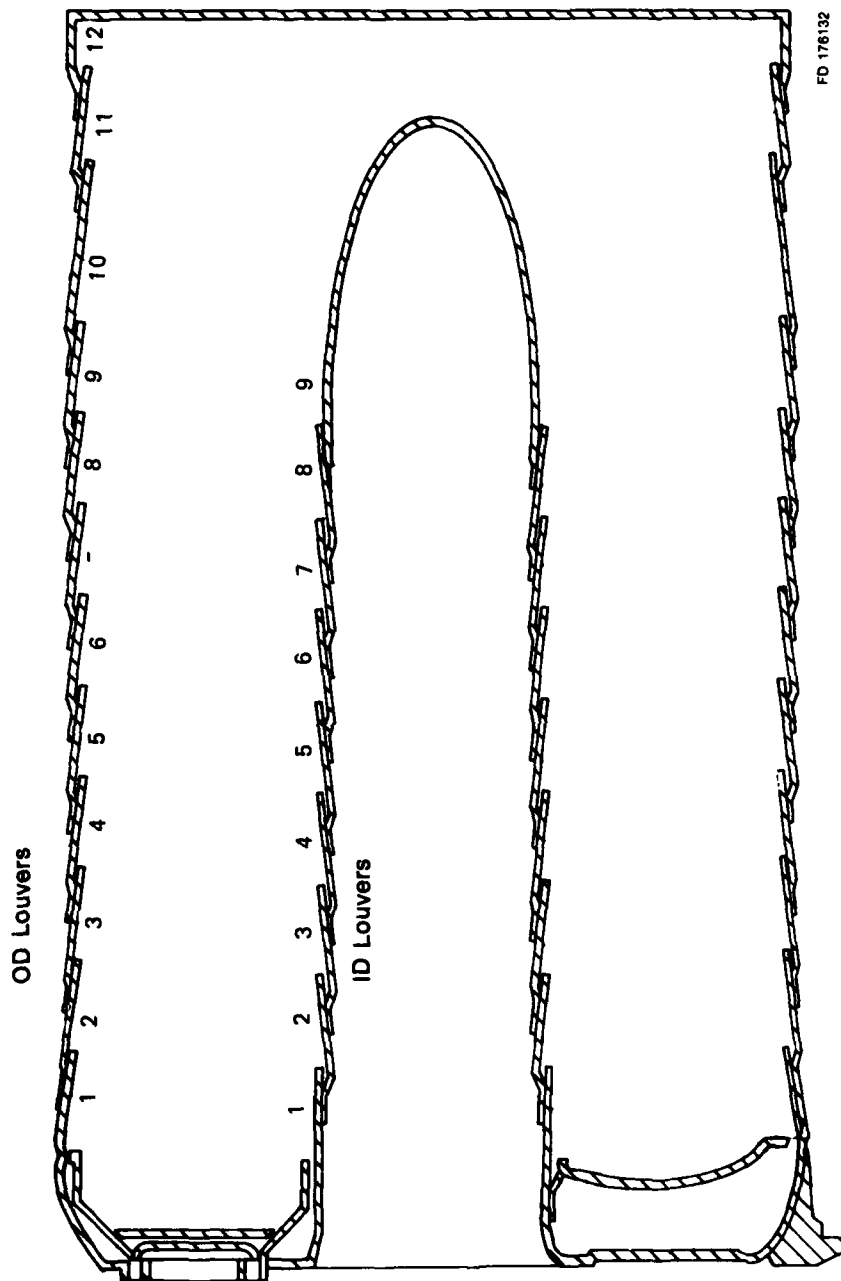
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Figure 61. Radiant Heat Loads Corresponding to Peak ID F100 Louver Temperatures at SLTO Conditions



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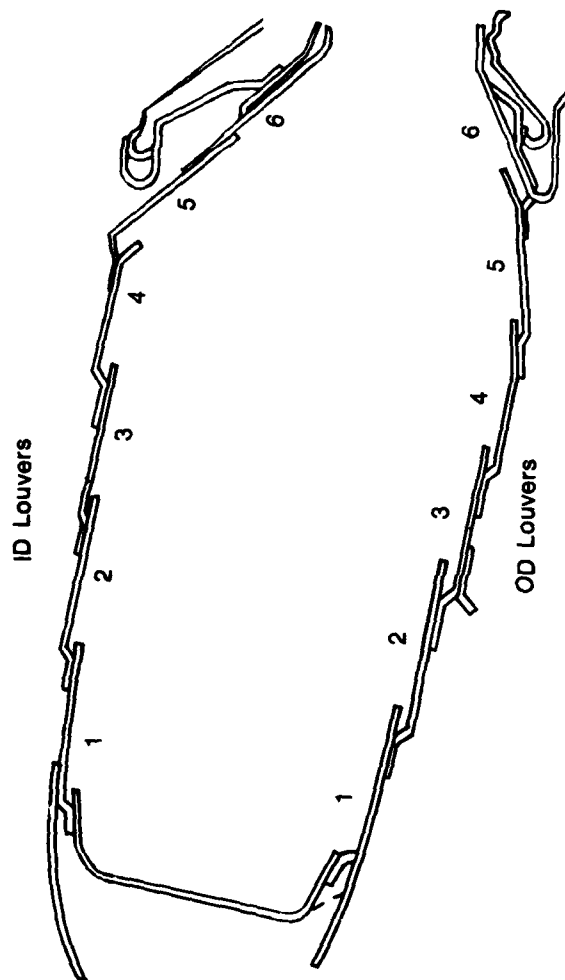
Figure 62. Radiant Heat Loads Corresponding to Peak OD F100 Louver Temperatures at SLTO Conditions



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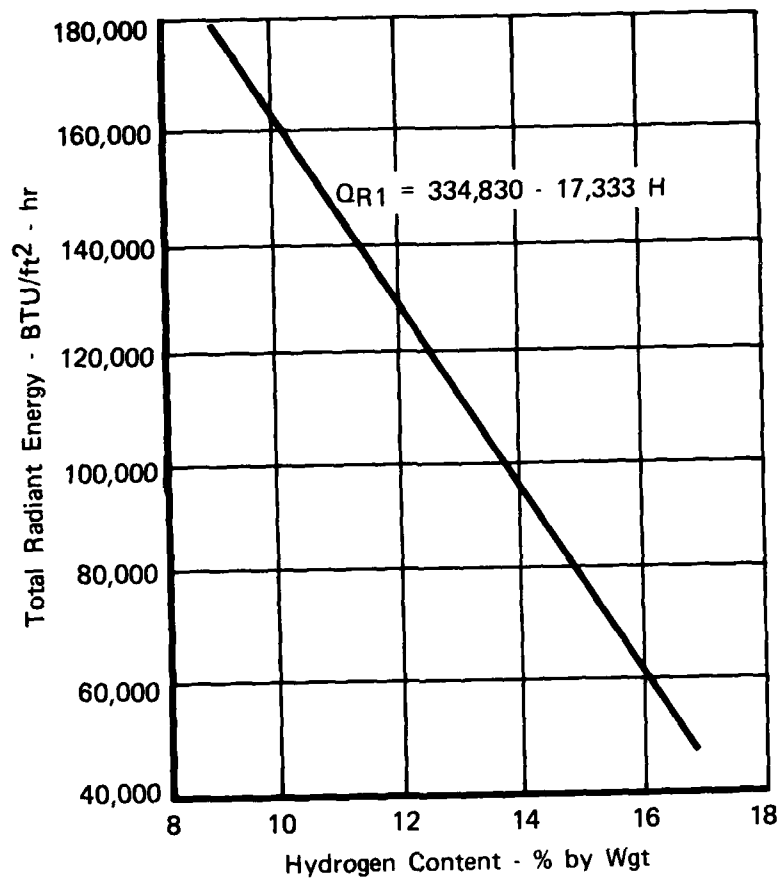
Figure 63. J57 Combustor — Cross-Sectional View

Engine



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Figure 64. F100 Combustor — Cross-Sectional View



FD 176126

Figure 65. Relationship of Hydrogen Content and Radiant Energy in a J57 Combustor

To date, no measurements of radiant heat loads in the F100 combustor have been obtained for fuels with varying hydrogen content. However, as discussed in Section III, the F100 combustor is expected to exhibit less sensitivity of radiant heat loads to hydrogen content than the J57 combustor. Therefore, Equation 30 was used to imply a change in radiant heat load for both the J57 and F100 combustors. The third step in the procedure was to add the increase in radiant heat loading found from Equation 30 to a base value of Q_{R1} found using the liner design program. Then, a new luminosity factor, and the corresponding liner temperatures were found. Base values of Q_{R1} were taken to be at a luminosity of 1.4. This value of luminosity has been shown to yield good agreement between measured and predicted liner temperatures. Inspection of Figures 55, 56, 59, and 60, however, reveals that the choice of a base luminosity has a negligible effect on the change in liner temperatures resulting from a given change in luminosity, since the slope of each individual curve is essentially constant in the range of $L = 1.0 - 1.6$. It should be mentioned that base values of Q_{R1} used were not average values found for the liners. An average value of Q_{R1} would include radiant heat loads calculated for portions of the liner that are shielded by louver lips and portions of the liner downstream of the primary zone, which are influenced only slightly by changes in luminosity. Instead, base values of Q_{R1} used in the procedure correspond to peak temperatures found in the primary zone of each combustor.

Calculated values for the increase in radiant heat loads are presented in Table 17. Values for both Case 1 (corresponding to a change in fuel properties from average or typical values to current specification limits) and Case 2 (corresponding to a change in fuel properties from average or typical values to the proposed specification limits) are shown. Luminosity values are given in Table 18 for both combustors, both fuels, and Case 1 and Case 2 conditions. As shown, the changes in radiant heat loads for JP-4 and JP-8 under Case 2 conditions did not differ significantly, and luminosity values for the two fuels are equal.

TABLE 17. CHANGES IN % HYDROGEN CONTENT AND LINER RADIANT HEAT LOAD

	JP-4		JP-8	
	ΔH (% by Wgt)	ΔQ_{R1} (B/hr-ft ²)	ΔH (% by Wgt)	ΔQ_{R1} (B/hr-ft ²)
Case 1	-0.18	3,120	-0.14	2,430
Case 2	-0.24	4,160	-0.25	4,330

TABLE 18. EFFECT OF RELAXED FUEL SPECIFICATIONS ON LUMINOSITY

	F100(3)		J57-59W	
	JP-4	JP-8	JP-4	JP-8
Base	1.40	1.40	1.40	1.40
Case 1	1.44	1.43	1.48	1.47
Case 2	1.45	1.45	1.51	1.51

Results

The increase in average liner temperature resulting from a change in fuel properties corresponding to Case 2 conditions is shown plotted against combustor inlet temperature in Figures 66 and 67 for the J57 and F100 combustors, respectively. As shown, the thermal model used in the analysis predicts that the maximum increase in average liner temperature occurs at SLTO operating conditions for the J57 combustor, and at cruise operating conditions for the F100 combustor. Therefore, the effect of increasing luminosity (increasing fuel aromaticity) is greatest in the J57 combustor at engine operating conditions which result in the highest average liner temperature. This indicates that a given decrease in fuel hydrogen content will have a greater impact on liner distress problems in the J57 combustor than in the the F100 combustor.

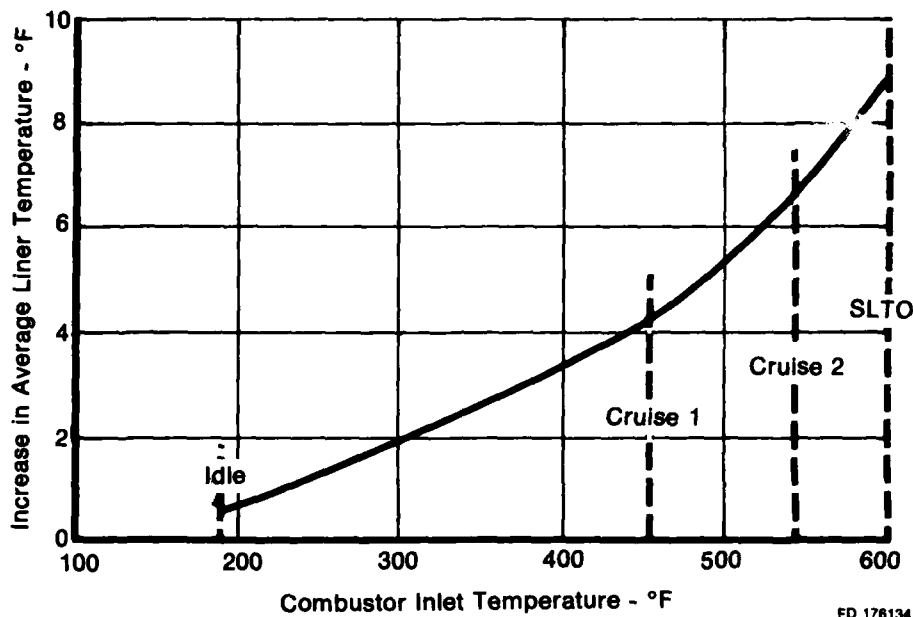


Figure 66. Increase in Average Liner Temperature Resulting from a Change in Fuel Properties Corresponding to Case 2 Conditions vs Inlet Temperature in a J57 Combustor

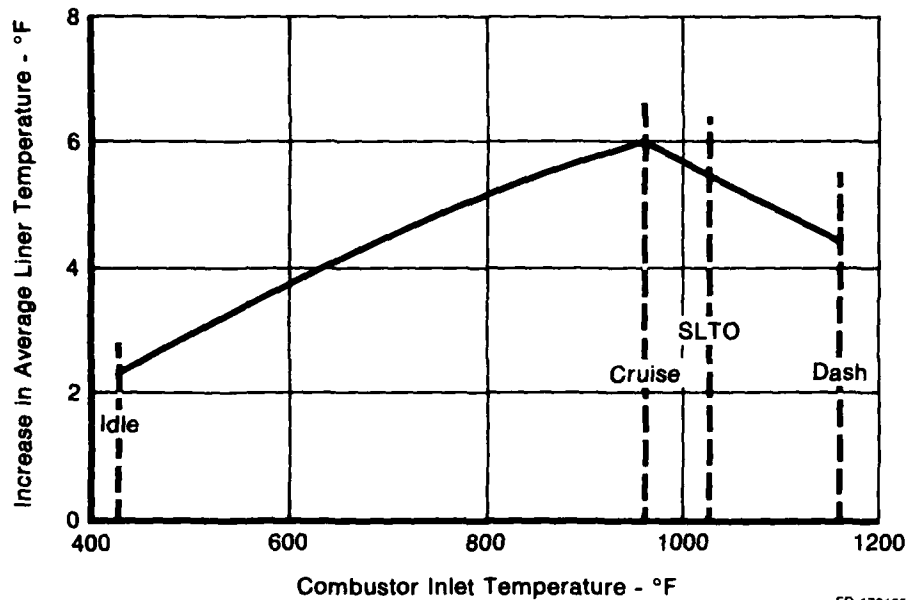


Figure 67. Increase in Average Liner Temperature Resulting from a Change in Fuel Properties Corresponding to Case 2 Conditions vs Inlet Temperature in an F100 Combustor

Increases in average liner temperature may also be expressed using a dimensionless liner temperature parameter (LTP) defined as

$$LTP = \frac{T_L - (T_L)_b}{(T_L)_b - T_{in}} \quad (31)$$

where

$T_L - (T_L)_b$ = Increase in liner temperature relative to the base fuel

$(T_L)_b$ = Average liner temperature with the base fuel

T_{in} = Combustor inlet temperature.

The liner temperature parameter has been used successfully by Blazowski (Reference 52) to correlate increases in liner temperature with fuel hydrogen content for a number of mature engine designs, including the J57. Values of LTP for the J57 and F100 combustors were calculated using the average liner temperatures predicted by the liner design program for Case 2 fuel property changes. These calculated values are shown in Tables 19 and 20 for each engine power level. Liner temperature parameters at cruise conditions (Cruise 2 conditions for the J57 combustor) are compared with the Blazowski correlation in Figure 68. All data shown in Figure 68 is based on JP-4 fuel with a hydrogen content of 14.5%.

TABLE 19. CALCULATED LINER TEMPERATURE PARAMETERS FOR THE F100(3) COMBUSTOR

Power Level	LTP
Idle	0.004
Cruise	0.005
SLTO	0.004
Dash	0.003

TABLE 20. CALCULATED LINER TEMPERATURE PARAMETERS FOR THE J57-59W COMBUSTOR

Power Level	LTP
Idle	0.009
Cruise 1	0.013
Cruise 2	0.026
SLTO	0.023

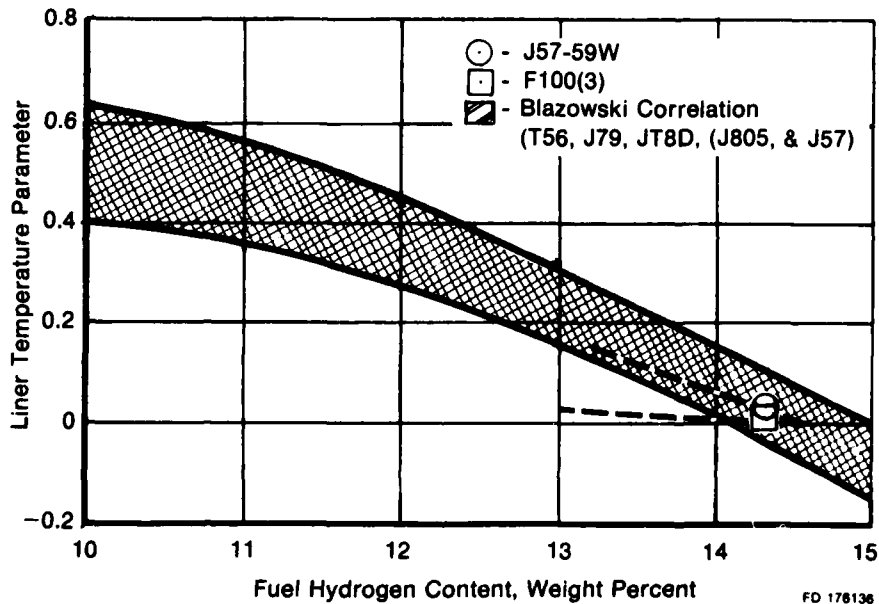


Figure 68. J57 Liner Temperature Parameter at Cruise Condition Compared With the Blazowski Correlation

The LTP calculated for the J57 combustor shows excellent agreement with data correlated for the mature combustors. The calculated LTP for the F100 combustor, however, indicates that a decrease in fuel hydrogen content has a much smaller effect on F100 liner temperatures than on mature engine liner temperatures. This difference is not unexpected, however, since results of a recent Air Force sponsored program (Reference 55) indicated that no increase in liner temperatures resulted from operation of an F100 engine on both JP-4 and Jet A fuels. The difference in fuel hydrogen content between JP-4 and Jet A fuels is approximately 0.6%, as compared to the difference of 0.24 to 0.25% resulting from the proposed changes in the JP-4 and JP-8 fuel specifications being considered in this study.

Increases in the average temperatures of individual louvers at SLTO operating conditions due to the relaxed specification fuels are shown in Figures 69 and 70 for the J57 combustor, and in Figures 71 and 72 for the F100 combustor. Both Case 1 and Case 2 fuel property changes are illustrated in these figures. As mentioned previously, overall (Case 2) changes for both JP-4 and JP-8 fuels yield the same effect on liner temperature increases. However, Case 1 changes in liner temperatures are not equal. The difference between average JP-4 fuel properties and current JP-4 specifications accounts for approximately 80% of the overall liner temperature rise, while the difference between typical JP-8 fuel properties and current JP-8 specifications accounts for approximately 60% of the overall liner temperature rise.

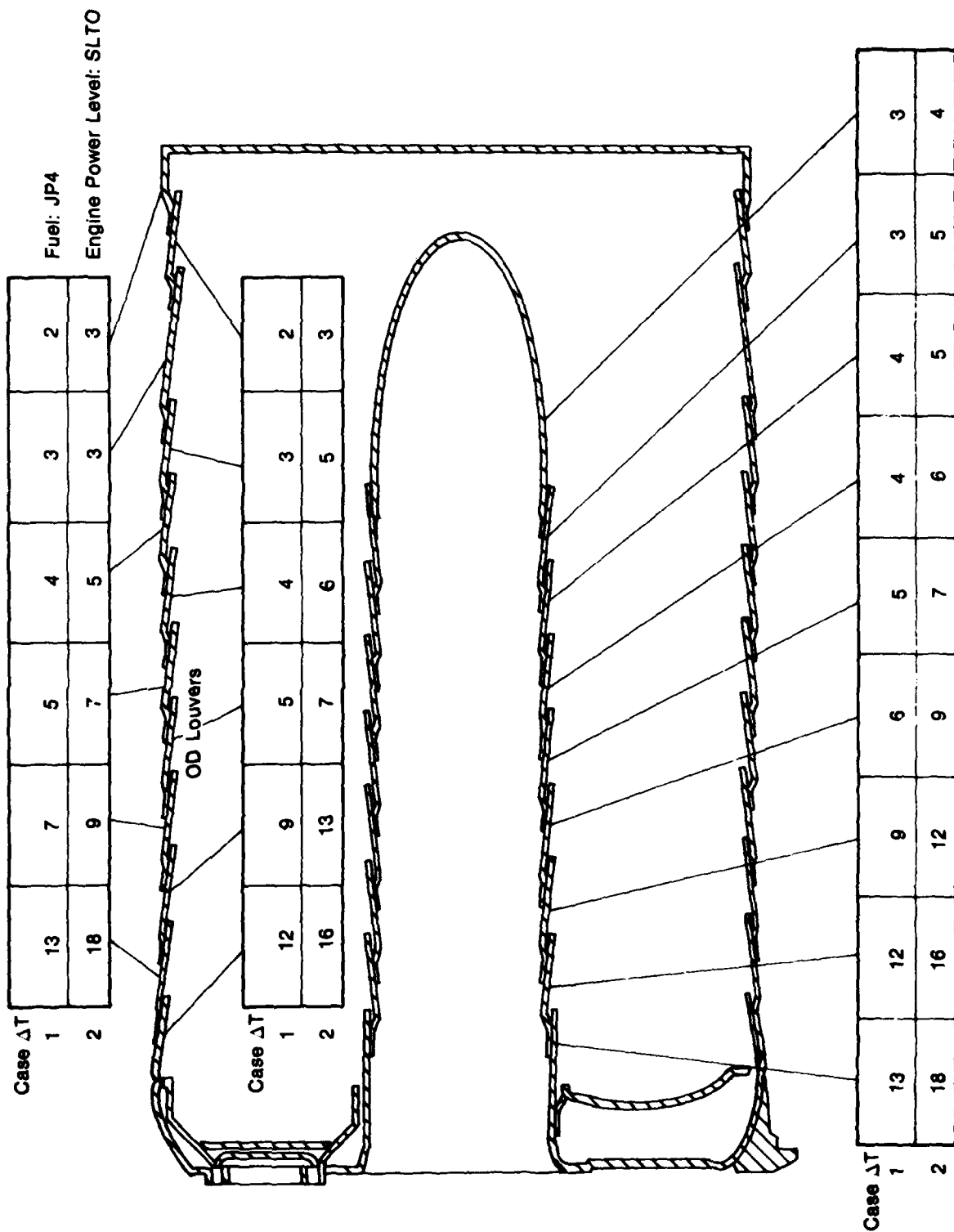
Predicted increases in peak louver temperatures for the J57 and F100 combustors are shown in Figures 73 through 76 at SLTO conditions. Again, the effect of both Case 1 and Case 2 fuel property changes is illustrated. Increases in peak louver temperatures are approximately 50 to 60% greater than average louver temperatures for both combustors.

A comparison of the predicted increases in J57 and F100 louver temperatures shown in Figures 69 through 76 indicates that an increase in fuel aromaticity will have a much more pronounced effect on liner durability problems with the J57 combustor than with the F100 combustor. This is particularly evident in louvers located near the primary zone of the J57 combustors. Increases in peak louver temperatures in the primary zone of the J57 combustor are approximately 70 to 270% greater than the overall liner temperature increase. Increases in F100 liner temperatures, on the other hand, are fairly uniform with axial distance along the combustor.

TURBINE THERMAL ANALYSIS

As mentioned previously in Section III, the use of the relaxed specification JP-4 and JP-8 fuels in gas turbine combustors is not expected to change combustor exit temperature pattern factors or radial temperature profiles. Therefore, the turbine thermal analysis performed in this study was limited to a determination of the effect of increased hot gas luminosity on inlet guide vane radiant heat loads and surface temperatures. Radiant heat loads to turbine inlet guide vanes are not normally considered to have a significant impact on turbine durability. Due to high local velocities at inlet vane surfaces (local Mach numbers approach a value of 1.0), convective heat transfer coefficients, and thus convective heat loads, are high. In addition, flame-to-inlet vane viewing angles (especially in the case of can-annular combustion systems) are relatively small. Therefore, radiant heat loads account for a small portion of total inlet vane heat loads.

This fact is indicated by Figures 56, 57, 61, and 62, where it is shown that radiant heat loads to aft combustor louvers are much smaller than those found for upstream louvers and, in addition, exhibit less sensitivity to increases in flame luminosity. The thermal analysis performed in this study, although non-rigorous in nature, is intended to estimate the approximate values of radiant and convective heat loads on inlet guide vanes for the J57 and F100 combustors, and to further illustrate the negligible effect of increased flame luminosity on surface temperatures.



FD 176137

Figure 69. Effect of JP-4 Fuel Specification Relaxations on Increase in Average J57 Combustor Liner Temperatures

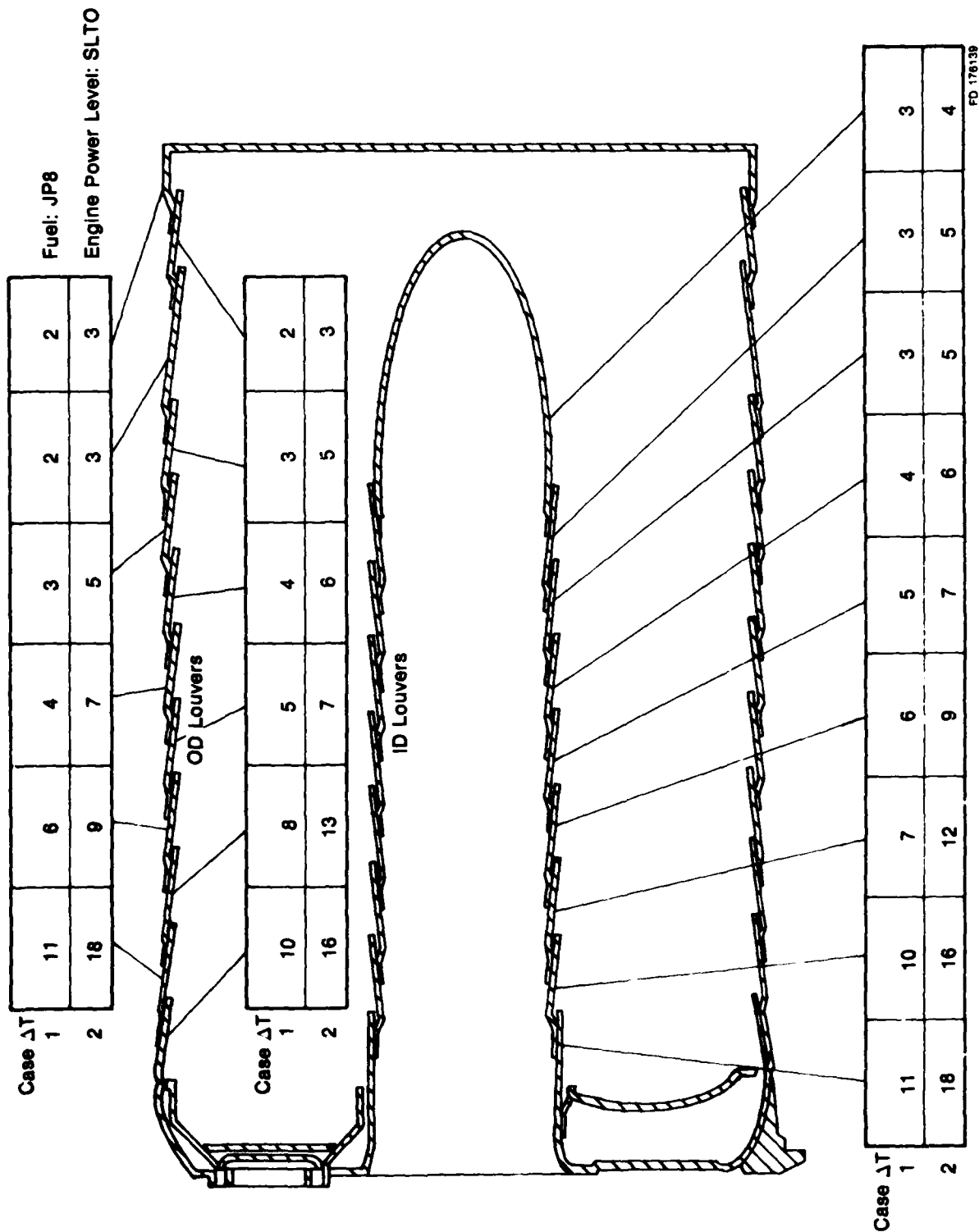
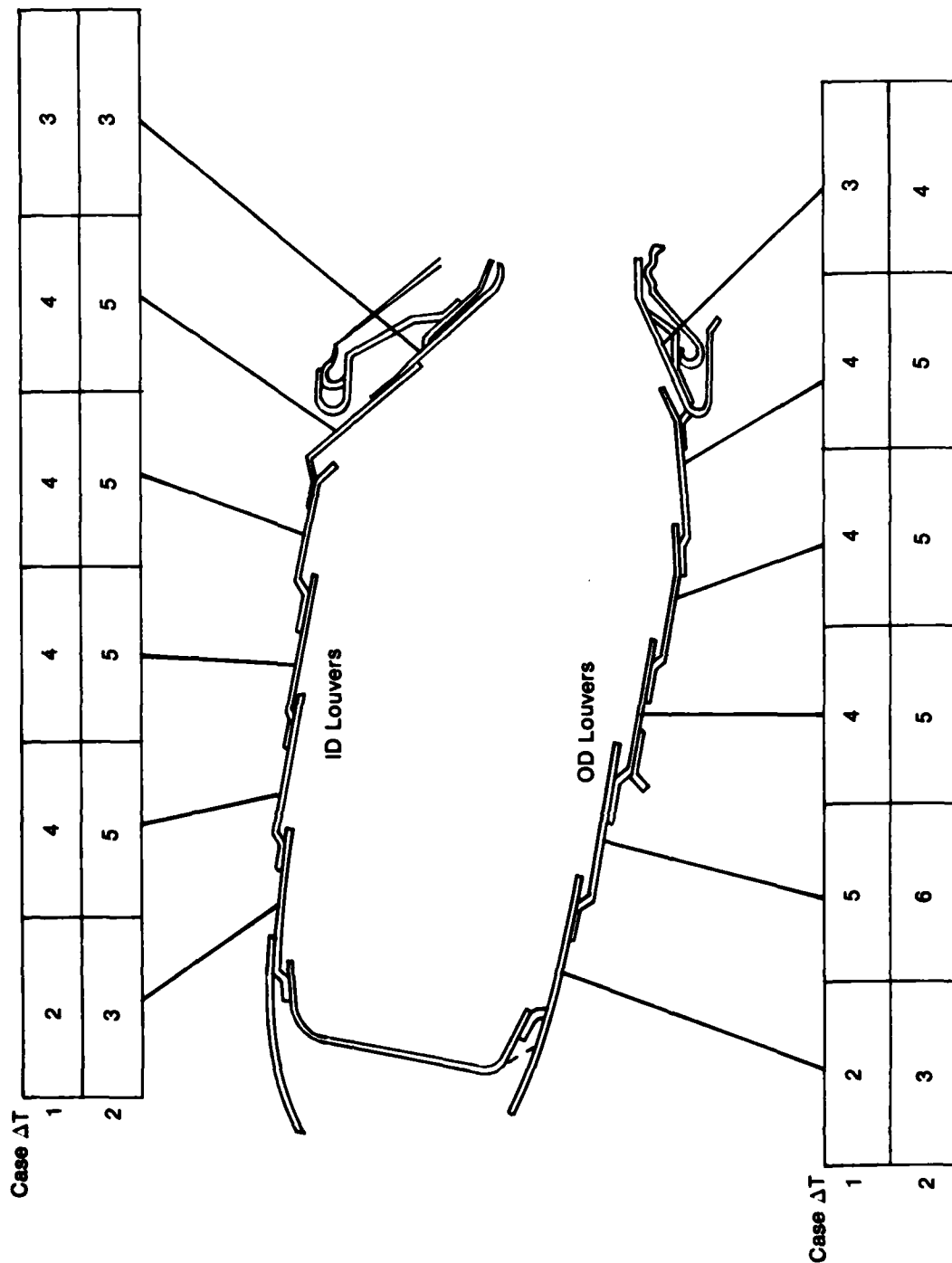


Figure 70. Effect of JP-8 Fuel Specification Relaxations on Increase in Average J57 Combustor Liner Temperatures

Fuel: JP4

Engine Power Level: SLTO



FD 176139

Figure 71. Effect of JP-4 Fuel Specification Relaxations on Increase in Average F100 Combustor Liner Temperatures

Fuel: JP8
Engine Power Level: SLTO

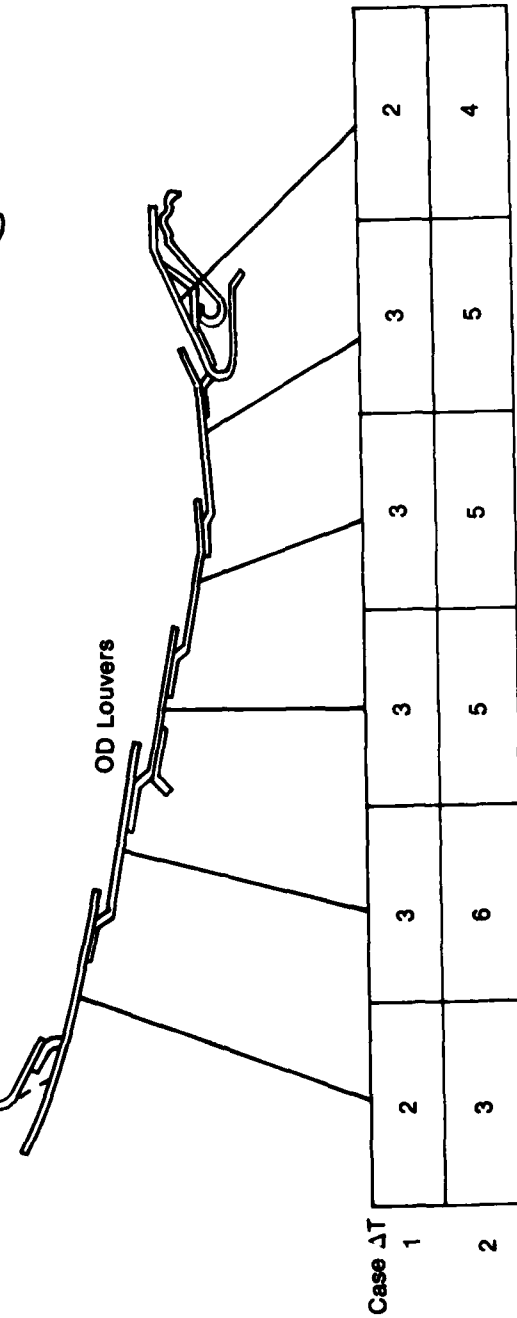
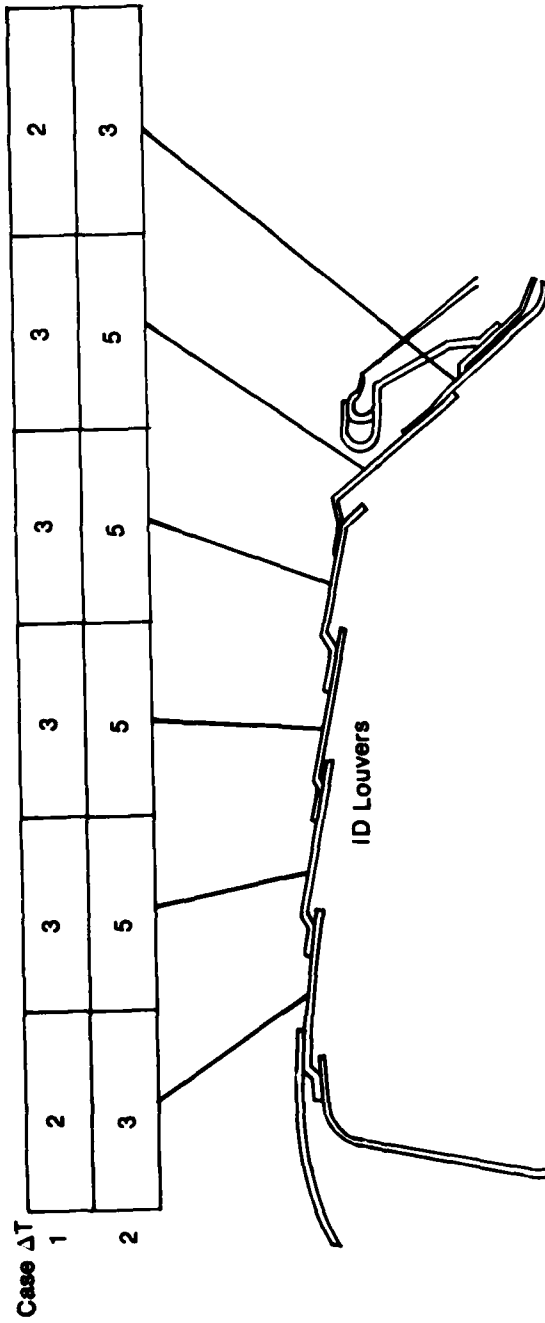


Figure 72. Effect of JP-8 Fuel Specification Relaxations on Increase in Average F100 Combustor Liner Temperatures

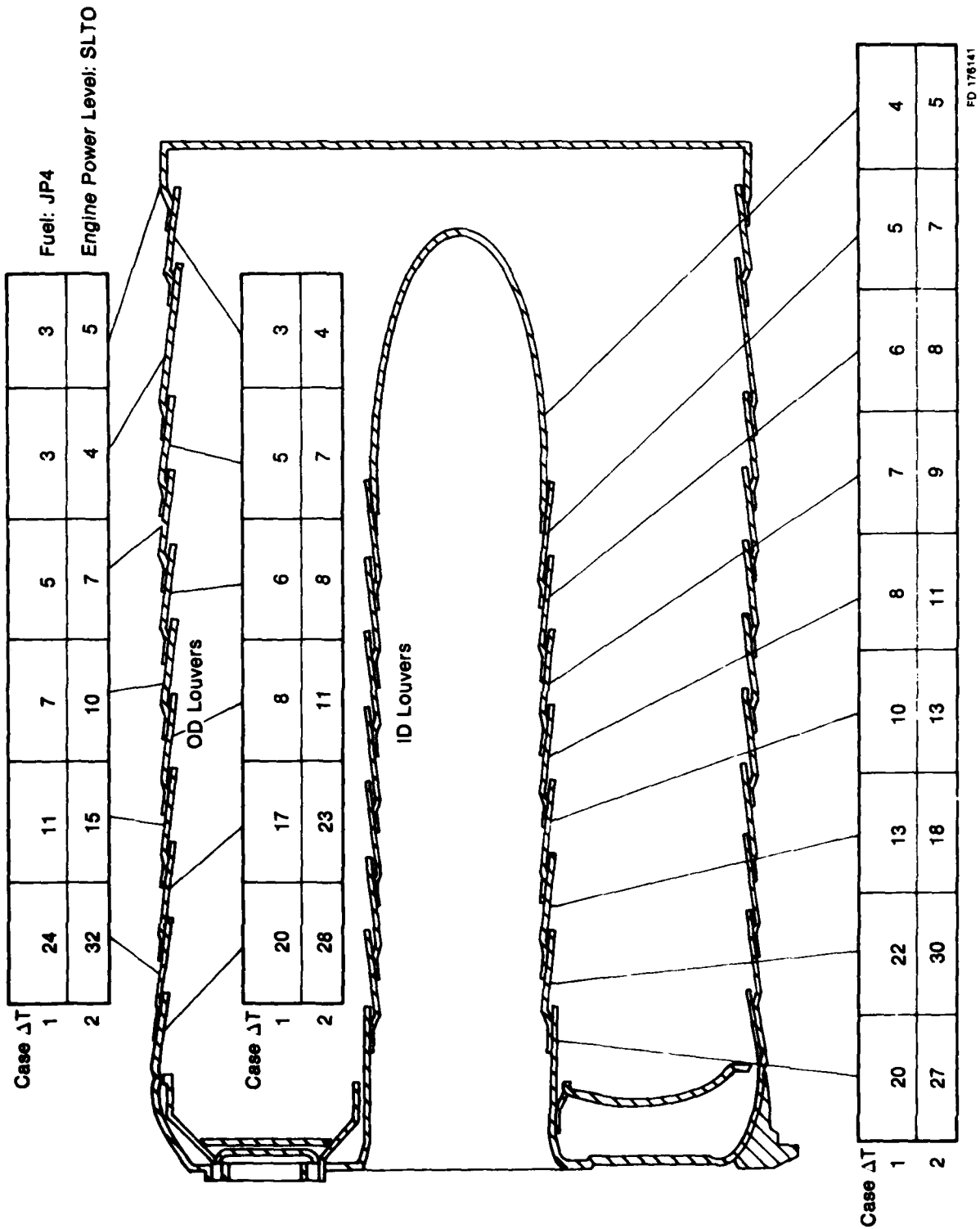
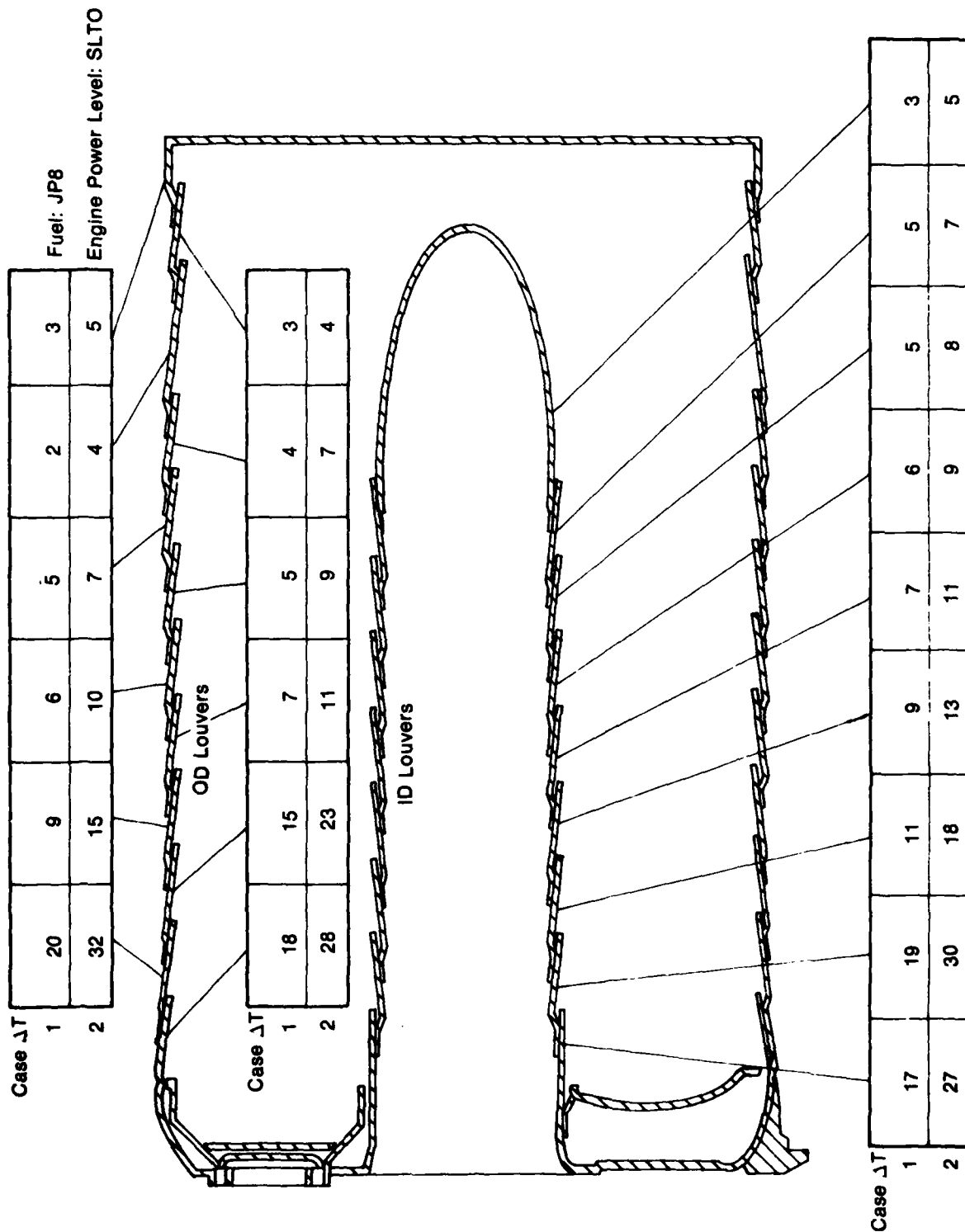


Figure 73. Effect of JP-4 Fuel Specification Relaxations on Increase in Peak J57 Combustor Liner Temperatures

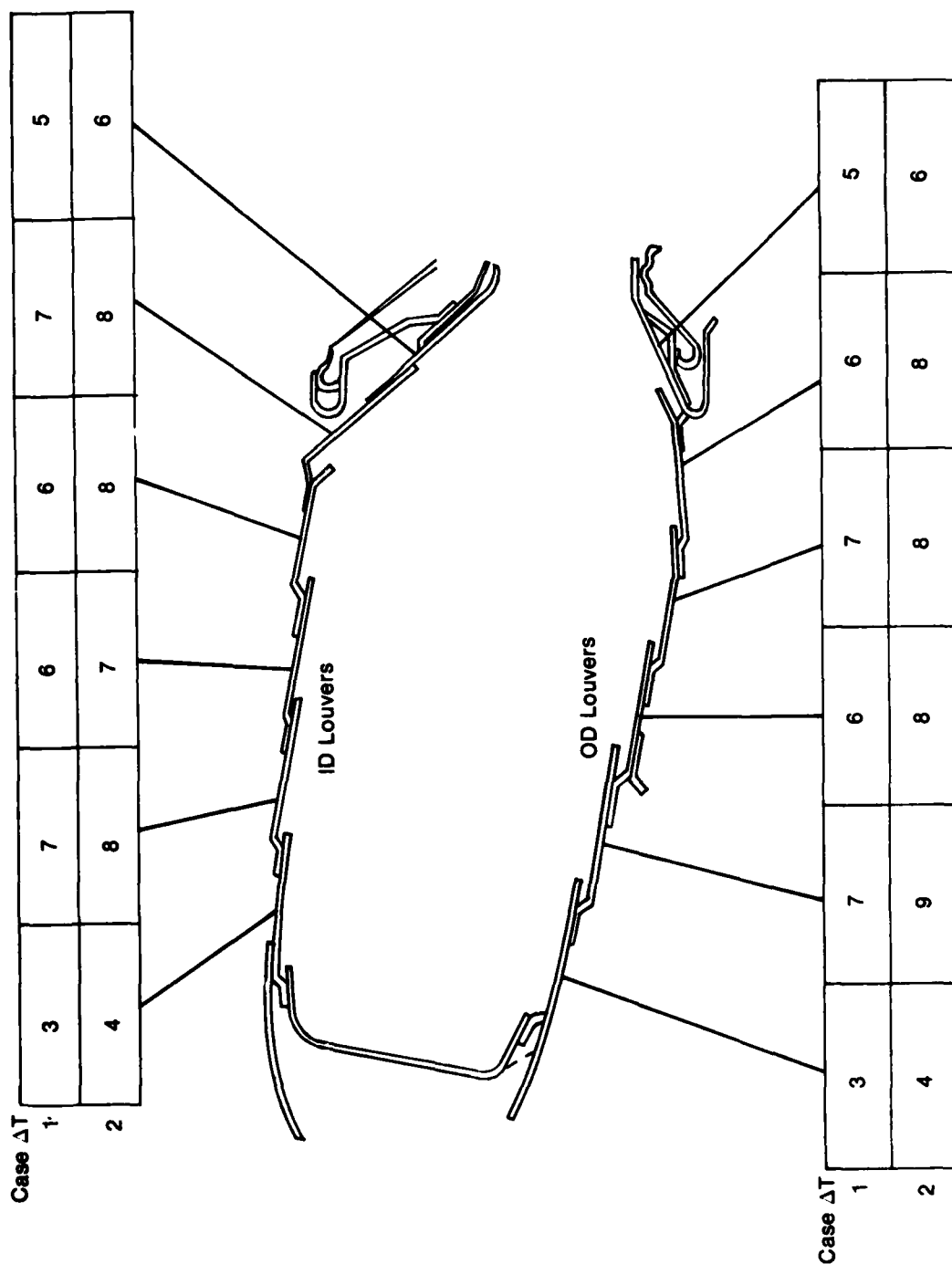


FD 178142

Figure 74. Effect of JP-8 Fuel Specification Relaxations on Increase in Peak J57 Combustor Liner Temperatures

Fuel: JP4

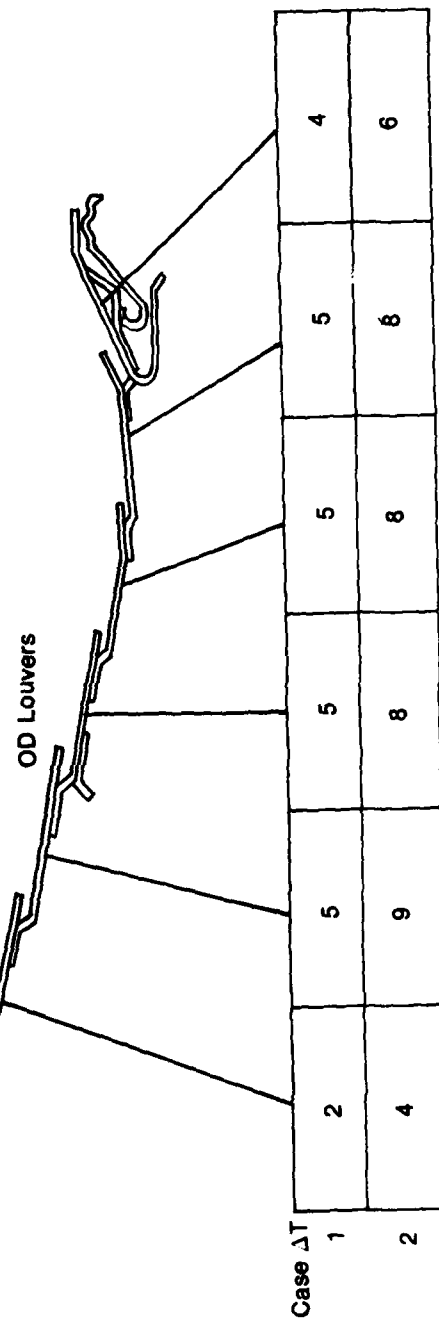
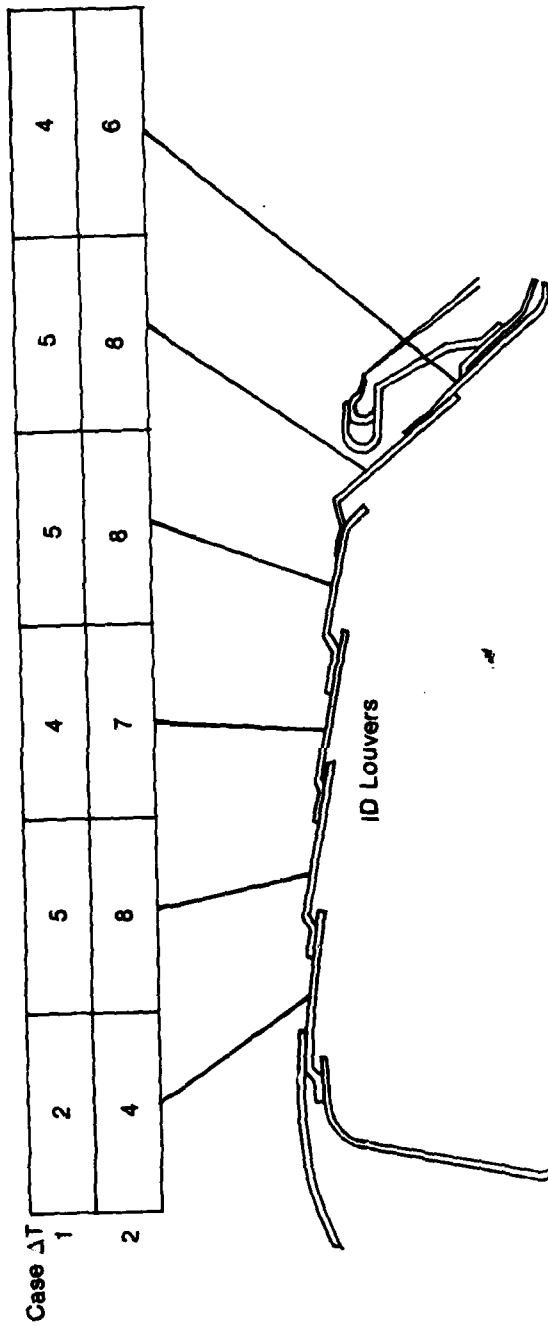
Engine Power Level: SLTO



FD 176143

Figure 75. Effect of JP-4 Fuel Specification Relaxations on Increase in Peak F100 Combustor Liner Temperatures

Fuel: JP8
Engine Power Level: SLTO



FD 176144

Figure 76. Effect of JP-8 Fuel Specification Relaxations on Increase in Peak F100 Combustor Liner Temperatures

Procedures and Basic Assumptions

Estimated values for radiant and convective heat loads to the inlet guide vanes were calculated for both engines at SLTO operating conditions. A luminosity factor of 1.4 was used to establish base radiant heat loads, and luminosity factors of 1.45 and 1.51 (Case 2 conditions) were used to establish radiant heat loads corresponding to the relaxed specification fuels for the F100 and J57 inlet vanes, respectively.

Radiant heat transfer to inlet guide vanes was found using the following equation:

$$(Q_R)_{IV} = \sigma_{sb} A_{IV} F_{C-IV} \left[\frac{1 + \epsilon_{IV}}{2} \right] [\epsilon_g T_g^4 - \alpha_g T_{IV}^4] \quad (32)$$

where

- σ_{sb} = Stefan-Boltzmann constant, 0.1713×10^{-8} Btu/ft²·hr·°R
- A_{IV} = Inlet vane surface area
- F_{C-IV} = Radiation shape factor from radiating gas to inlet vanes
- ϵ_{IV} = Emissivity of inlet vanes (assumed equal to 0.8)
- ϵ_g = Emissivity of hot gases
- α_g = Absorptivity of hot gases
- T_g = Hot gas temperature
- T_{IV} = Inlet vane temperatures.

Hot gas emissivity and absorptivity were found using the correlations shown previously in Equations 26 and 27, with the exception that T_{IV} was used in place of T_w in Equation 27.

The major assumptions involved in calculating values for $(Q_R)_{IV}$ were:

- Average radiation shape factors were calculated midway between the front end of each combustor and the inlet guide vanes
- Hot gas temperatures were assumed to be the peak flame temperature found to occur in the primary zone (predicted using the liner design program)
- Attenuation was neglected
- Inlet vane surface area was assumed to be the annular area of the vanes projected in a plane perpendicular to the engine centerline.

Based on the assumptions listed above, calculated values of the inlet vane radiation loads represent conservative estimates.

Calculated radiation shape factors from the combustor hot gases to the inlet vanes were found to be 0.20 and 0.04 for the F100 and J57 engines, respectively. The F100 annular combustor design allows inlet vanes to have a greater view of the flame than the can-annular design of the J57 combustor, where most of the radiant energy is transferred to combustor walls and transition duct.

Convective heat loads were determined using the following equation:

$$(Q_C)_{IV} = A_{PS} h_{PS} (T_g - T_{IV}) \quad (33)$$

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FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY. (U)

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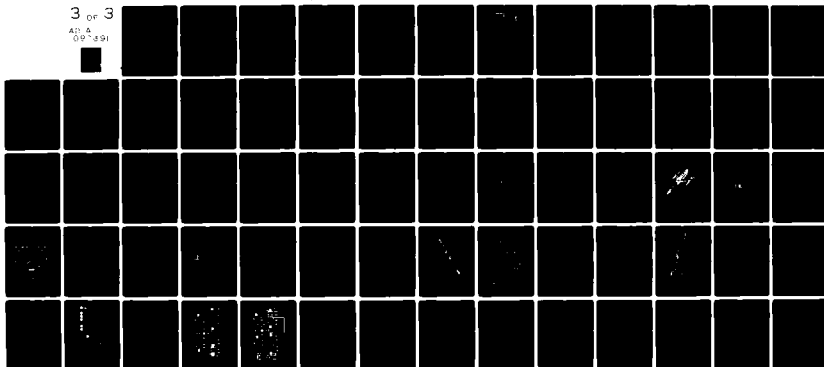
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where

A_{PS} = Surface area of the inlet vanes (concave or pressure sides only)

h_{PS} = Average convective heat transfer coefficient on the concave side of the inlet vanes.

Inlet vane convective heat loads were assumed to remain constant with the changes in luminosity used in the analysis. As will be shown later, temperature increases due to a change in luminosity are negligible, and the assumption of constant Q_c does not introduce any significant error into the analysis method. Typical values used for the convective heat transfer coefficient in Equation 33 are 550 B/hr-°F-ft² for the F100, and 500 B/hr-°F-ft² for the J57. Average vane temperatures used in Equation 32 (and 31) were approximately 1900°F and 1400°F for the F100 and J57, respectively.

Results

Results of the inlet guide vane thermal analyses for both engines are summarized in Table 21. As was expected, radiant heat loads are small in comparison with convective heat loads, accounting for approximately 8% of the total heat load to F100 inlet vanes, and approximately 2.5% of the total heat load to J57 inlet vanes. Increases in the total inlet vane heat loads due to a change from average of typical fuel properties to properties corresponding to the proposed fuel specifications (Case 2 conditions) were found to be insignificant, with values of less than 0.2% for either the F100 or J57 engine. Therefore, changes in inlet guide vane surface temperatures are negligible, being less than 1°F.

TABLE 21. INLET GUIDE VANE HEAT LOADS AT SLTO OPERATION CONDITIONS

Engine	Luminosity Factor	Q_R (B/hr)	Q_C (B/hr)	Q_T (B/hr)	Q_R/Q_T	Increase in Q_T (%)
F100(3)	1.40	48,340	555,560	603,900	0.080	—
	1.45	49,390	555,560	604,950	0.082	0.17
J57-59W	1.40	7,980	326,300	334,280	0.024	—
	1.51	8,290	326,300	334,590	0.025	0.09

SECTION V

COMBUSTOR AND TURBINE LIFE PREDICTIONS

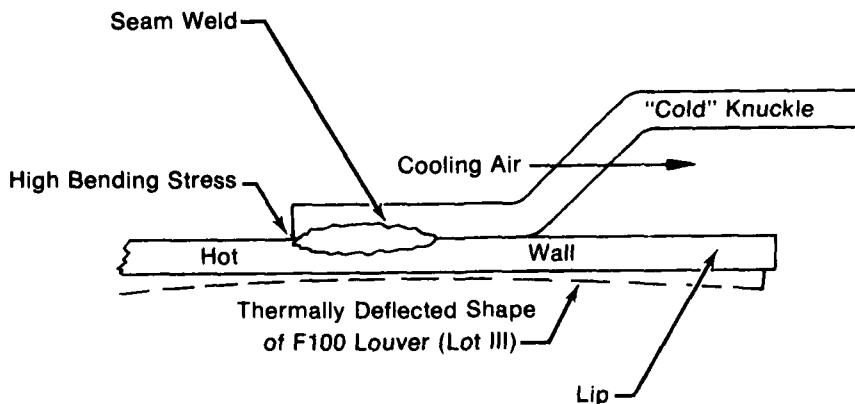
COMBUSTOR LIFE PREDICTIONS

Increases in F100 and J57 liner temperatures, presented in Section IV, are not of sufficient magnitude to result in predicted peak or average louver temperatures excessive enough to yield oxidation/erosion problems with Hastelloy-X liner materials. The thermal model used for the analysis, however, does not predict local peak temperatures which occur due to circumferential nonuniformities in the combustor flow field (hot streaks) or variations in the combustor geometry such as crossover tubes or igniters. Therefore, to determine the effect of relaxed fuel specifications on combustor life, the predicted increases in liner temperatures were applied to areas of distress known to exist from field experience in the J57 and F100 combustors.

It should be emphasized that the following life predictions for the J57 and F100 combustors do not represent results of in-depth analyses. The study was made based on limited existing information that relates changes in metal temperature and estimated life and should be considered to be engineering estimates only.

F100(3) Combustor Life Predictions

The F100 combustor life is currently limited by circumferential cracking in the 5th to 6th ID louver seam weld caused by low-cycle fatigue (LCF). LCF results from cyclic expansion and contraction of the combustor liner during engine operation. Initiation of cracks occurs at locations in the liner where high stress exists due to temperature gradients. This location is usually at the seam weld between two adjacent louvers, as shown in Figure 77, where a relatively large temperature gradient (approximately 200 to 400°F) exists between the louver wall and knuckle. At high-power conditions, the stress concentrations in the vicinity of the seam weld are well above the yield strength of the material causing plastic deformation with each cycle. Cracks initiate in the weld material, go through the wall, and then grow circumferentially. The F100 combustor can continue to operate with large cracks of cumulative length up to two-thirds of the circumference; in terms of useful service, this results in a 36-in. total crack length.



FD 178402

Figure 77. Linear Location of High Stress Due To Temperature Gradients

Referring to Figure 77, the louver lip is exposed to the flame, acting as a radiation shield for the knuckle. The temperature of louver cooling air (entering in the vicinity of the knuckle) remains essentially constant with changes in fuel properties. Therefore, an increase in flame luminosity increases the hot louver wall temperature but not the knuckle temperature. This increases the thermal gradient causing higher bending stresses in the seam weld. A life effect estimate was made based on previous parametric life studies of the F100 burner which provides a relationship between combustor metal temperatures and combustor life (crack initiation). As shown in Figure 78, a change in flame luminosity from 1.40 to 1.45, corresponding to the effect of the proposed relaxations in JP-4 and JP-8 fuel specifications, causes a 5°F increase in the hot louver wall temperature at SLTO conditions. This yields a decrease in predicted life to crack initiation of approximately 2%. But since the combustor is actually allowed to crack up to 36 in., this 2% decrease is only part of the overall life effect. To estimate the effect of increased temperature (stress) on combustor useful service life (i.e., life to a 36-in. crack), it was assumed that the higher temperatures affect the initiation and propagation lives by the same amount. This assumption is partially supported by AMT and field data which indicates that the time to initiation is approximately the same as the time required to grow a cumulative crack length of 36 in. Therefore, the relative life vs luminosity relationship of Figure 79 applies to overall life as well as initiation life. It should be mentioned that no design analysis method currently exists for predicting crack growth (time beyond initiation) in combustors.

J57-59W Combustor Life Predictions

Results of the thermal analysis indicated that relaxed fuel specifications would have a greater impact on durability of the J57 combustor than on the F100 combustor. However, the J57 burner is not life limited to the same extent as the F100. The J57 louvered combustor cans can be weld repaired and have individual louvers replaced as often as required and are generally not considered to be a problem area. As a result, baseline life for the J57 combustor cannot be determined. In addition, the J57 combustor environment is less severe than the F100, at least for the current transport engine application (KC-135A) under consideration, and the J57 burner average liner temperature is approximately 350°F cooler than for the F100 (at SLTO conditions).

The distress feature that most often occurs in the J57 is erosion of the crossover tube boss ("armpit" burn), illustrated in Figure 80; secondly, circumferential cracking occurs at louver seam welds. The increase in temperature due to luminosity would cause cracks to start sooner, and thus, be larger when repaired at scheduled overhaul periods, but such earlier cracking should not drive the engine to shorter overhaul periods due to the relatively easier combustor environment and the relatively small increases in temperature.

The majority of cracks, when they occur, are at the 9th OD louver seam weld which was taken as the characteristic location for the crack life estimates presented herein. The increase in luminosity from 1.40 to 1.51, corresponding to the effect of the proposed fuel specification relaxations, causes a 6.5°F increase in hot louver wall temperature at the 9th louver seam weld, as shown in Figure 81. This causes higher stresses and reduced time to crack initiation by approximately 4.5%, as shown in Figure 82.

The higher temperature should also cause greater crack growth. Some crack growth data for a J57-P420 engine, a Navy F-8 fighter engine which runs hotter (burner exit temperature is approximately 200°F higher at SLTO conditions), were correlated with the P59W temperatures and stresses (conservative). The correlation yielded an estimated 2.5% decrease in cycles to a 2-in. crack size at a luminosity of 1.51 (Figure 82). Since the technical order (T.O. 21-J57-56) requires all cracks to be repaired no matter how small, the crack growth data analysis indicates that growth should not be significantly affected by increased luminosity (temperature), and thus, should not cause any additional failure modes.

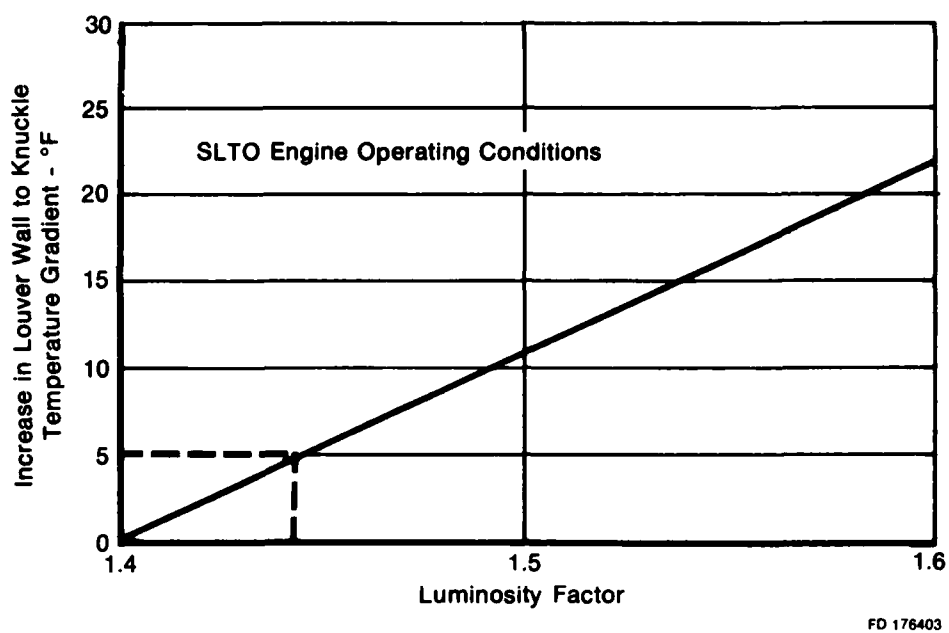


Figure 78. Increase in 6th Louver Temperature Gradient With Luminosity for F100 Combustor

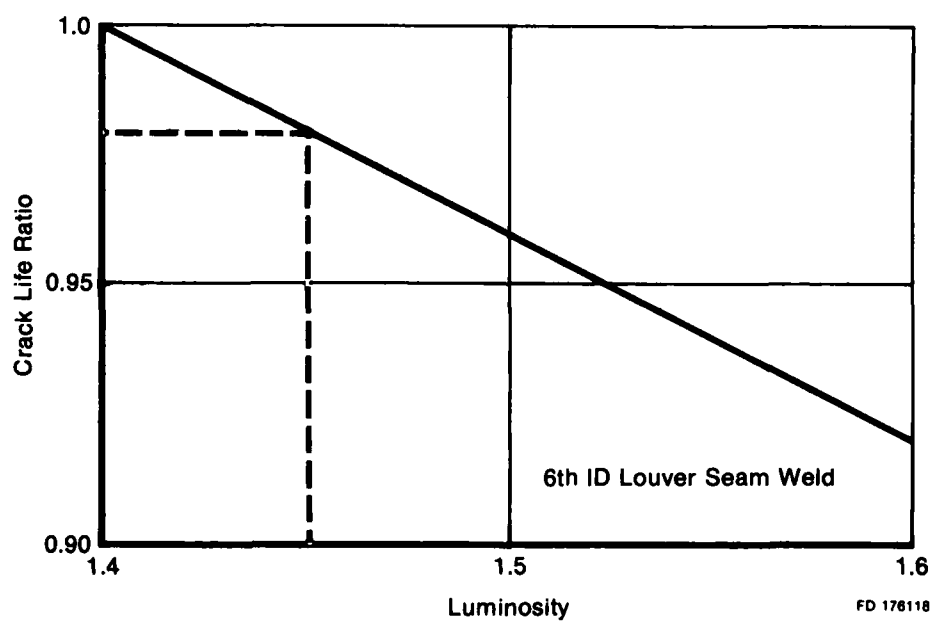
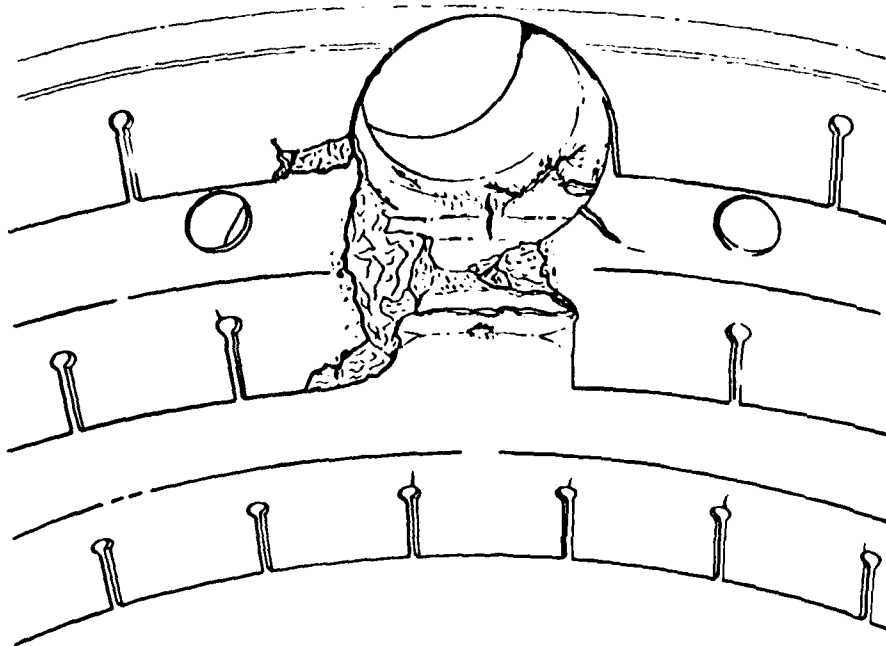
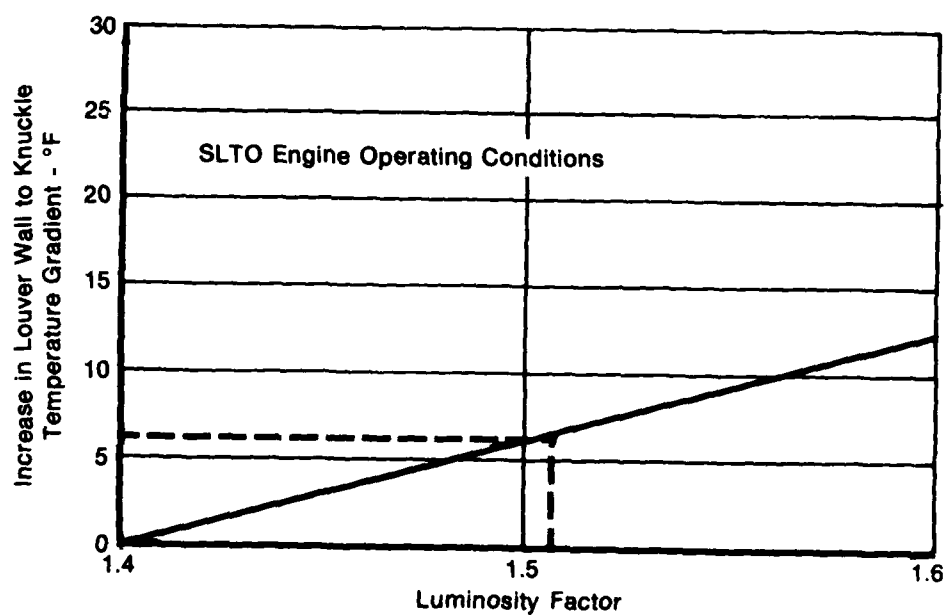


Figure 79. Relative LCF Crack Life vs Luminosity Relationship



FD 176148

Figure 80. Severe Erosion of Cross-Over Tube Boss in J57 Combustor



FD 176151

Figure 81. Increase in 9th Louver Temperature Gradient With Luminosity for J57 Combustor

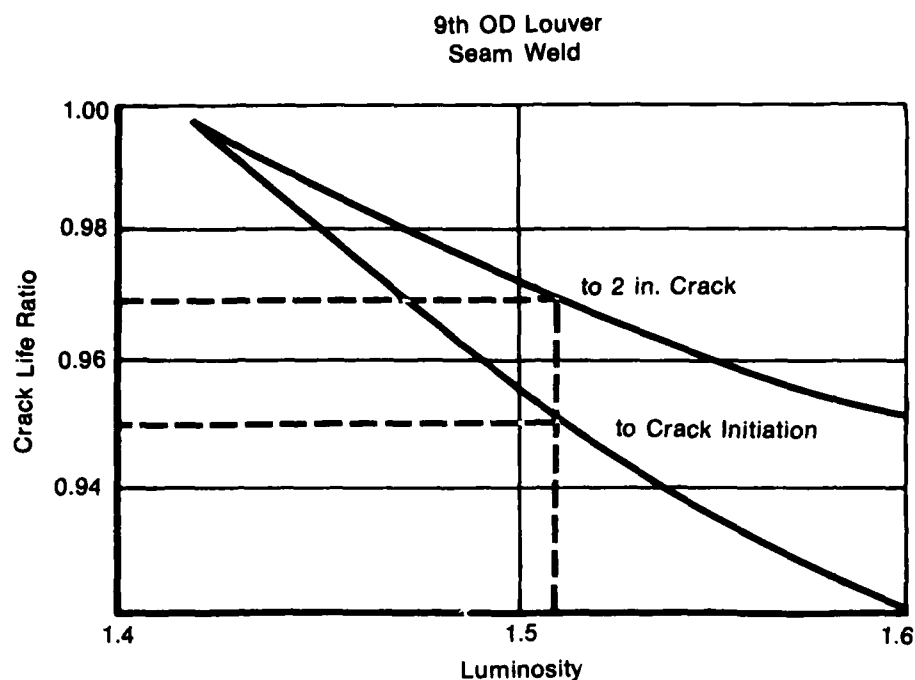


Figure 82. J57 Burner Estimated LCF Crack Life Ratio vs Luminosity

Increased erosion of the crossover tube boss could be more of a concern than cracking, because the uncooled boss, which is already at a relatively high temperature, is located near the main undiluted combustion zone and is more greatly affected by the large radiant heat load in that area which could cause as much as a 25-deg increase (predicted) in T_{metal} at SLTO. Exact metal temperatures of the crossover tube boss were unavailable, as was the mission (classified), making erosion life estimates difficult. However, depending on the metal temperature and based on available erosion data, a 25-deg increase in T_{metal} at SLTO (which would govern erosion life) could mean a life decrease of from 8 to 25% at $L=1.51$ (Figure 83). It should be mentioned that even this may not be a major concern since this part can also be easily replaced.

TURBINE LIFE PREDICTIONS

Three effects of relaxed specification fuels were considered with regard to turbine life: (1) increases in combustor exit temperature pattern factor and radial profile, (2) increases in radiant heat loading on inlet guide vanes, and (3) increases in particulate size. Based on available data found in the literature, burner exit pattern factors and radial profiles are not expected to be affected by relaxed specification fuels. The results of the inlet vane thermal analysis, presented in Section IV, indicated that increases in inlet vane temperatures would be negligible. There is insufficient data in the literature to determine the impact of fuel property changes on particulate size distribution; however, the changes in fuel properties under consideration in this study are expected to have a negligible effect on particulate size. Therefore, no reduction in turbine life is predicted due to the proposed fuel specification relaxations.

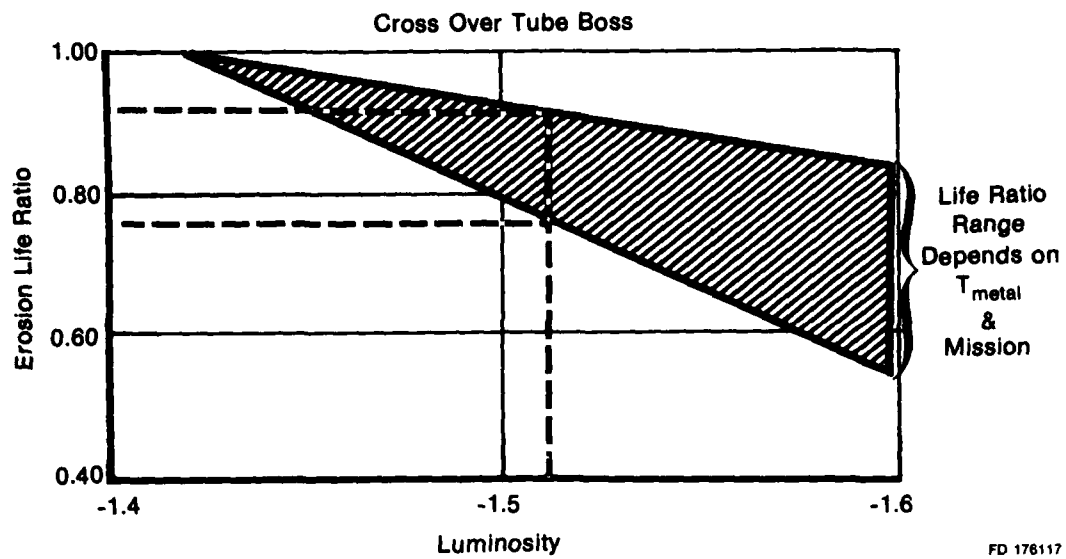


Figure 83. Cross-Over Tube Boss Estimated Erosion Life Ratio vs Luminosity

SECTION VI

PHASE I SUMMARY AND CONCLUSIONS

The overall objective of the Phase I effort was to assess the impact of broadened-specification fuels on the performance and durability of gas turbine engines used in USAF aircraft. The various engine related parameters addressed in this phase of the program included ignition characteristics, combustion efficiency, emissions, thermal loads, burner exit temperature distribution, erosion, and coking of the fuel system. The sensitivity of these parameters was discussed with regard to broadened-specification fuels in general, and with regard to the proposed relaxations of current JP-4 and JP-8 fuel specifications shown below:

JP-4: 14°F increase in freezing point
25°F increase in final boiling point

JP-8: 18°F increase in freezing point
25°F increase in final boiling point
2 mm decrease in smoke point

A fuel characterization study was performed to determine the effects of the proposed changes in JP-4 and JP-8 fuel specifications on fuel hydrogen content. Through the use of interproperty correlations, it was determined that a change from current JP-4 and JP-8 fuel values of final boiling point and smoke point to the proposed specification limits will decrease current fuel values of hydrogen content by 0.25 (% by weight). In addition, changes in other fuel properties, including volatility, specific gravity, viscosity, and thermal stability, implied by the proposed changes in JP-4 and JP-8 fuel specifications were estimated.

A literature survey was conducted to relate the chemical nature and physical properties of fuels to the engine related parameters mentioned previously. The impact of various fuel types on engine performance and durability was qualitatively discussed relative to three USAF engines: the F100, the TF30, and the J57. Whenever possible, estimates as to the extent of this impact were made utilizing the various fuel property changes determined in the fuel characterization study.

Thermal analyses were performed to analytically determine the effect of the proposed relaxations of JP-4 and JP-8 fuel specifications on combustor liner and turbine airfoil temperatures in two USAF engines: the J57-59W and the F100-PW-100. Increases in radiant heat loads to these engine components were found using the estimated change in fuel hydrogen content determined in the fuel characterization study and available data in the literature relating fuel hydrogen content and radiant heat loads. Increases in average liner temperatures resulting from the use of the relaxed specification JP-4 and JP-8 fuels relative to current JP-4 and JP-8 fuels were found to be approximately 9°F for the J57 combustor and approximately 6°F for the F100 combustor at sea level takeoff operating conditions. Increases in turbine airfoil temperatures were found to be negligible for both engines. The results of the thermal analyses were used to predict the corresponding impact on combustor life for the two engines considered. The major findings and conclusions of the Phase I effort with respect to durability and performance are given below.

- Durability

- The F100 combustor baseline life with current JP-4 and JP-8 fuels will be reduced by approximately 2% (or less) when using the relaxed specification JP-4 and JP-8 fuels.

- The J57 combustor is not life-limited to the same extent as the F100 combustor. Individual louvers are repaired and replaced as often as necessary, and a baseline life cannot readily be established. However, as a result of using the relaxed specification fuels relative to current fuels, cracks in the combustor liner will initiate approximately 4.5% (or less) sooner, and have approximately a 2.5% (or less) faster growth rate. In addition, the erosion rate in the vicinity of the cross-over tubes may increase by as much as 25%, depending on the exact KC-135 mission profile (classified).
- The relaxed specification JP-4 and JP-8 fuels are expected to have no impact on turbine durability in the F100 and J57 engines relative to current JP-4 and JP-8 fuels.
- The proposed relaxations of JP-4 and JP-8 fuel specifications are expected to have a negligible effect on fuel thermal stability. Therefore, coking of engine fuel systems should not increase.

- Performance

- The relaxed specification JP-4 and JP-8 fuels are expected to have no impact on engine performance, with the exception of ignition capability, relative to current JP-4 and JP-8 fuels. The higher viscosity and lower volatility of the relaxed specification fuels may have an adverse effect on ignition capabilities when fuel and/or air temperatures are relatively low (cold-day ground starts and altitude ignition). The extent of this effect depends on both operating conditions and the particular engine employed, and cannot be predicted due to a lack of pertinent data. However, the incremental effect of the relaxed specification fuels on ignition capabilities relative to current JP-4 and JP-8 fuels is expected to be less than the incremental effect associated with the use of JP-5 relative to JP-4 fuel.

SECTION VII

RECOMMENDATIONS

Based on the findings of the Phase I effort, the following recommendations for future fuel-related studies are made:

- Experimental programs should be conducted to determine the impact of fuel type on engine operation characteristics. Actual engine components should be employed in these programs, and the effects of fuel types exhibiting variances in both chemical and physical properties should be studied with regard to individual components and fuel-related parameters. Areas in particular need of further research include
 - The effects of various physical and chemical fuel properties on carbon particulate formation, and the resulting deposition of these particulates on combustor liners and turbine airfoils. Emphasis should be placed not only on total particulate loading, but also on particulate size distribution and its impact on combustor and turbine airfoil erosion tendencies.
 - Fuel thermal stability and its relation to coke formation in actual engine fuel systems.
 - The effect of fuel types on augmentor performance and durability.
- If the current trend in decreased hydrogen content of jet fuels is expected to continue, programs directed towards developing improved-durability combustor liner designs should be conducted. It should be emphasized that increasing the percentage of total burner airflow used for liner cooling is not a desirable means of compensating for higher radiant heat loads. Increasing liner cooling flow adversely affects burner exit temperature distribution with a resultant decrease in turbine durability.
- Several advanced combustor designs (References 29 and 53) have exhibited less sensitivity of liner temperatures to variations in fuel hydrogen content than conventional combustor designs. Therefore, programs should be conducted to experimentally develop combustors which exhibit low sensitivity to fuel hydrogen content for possible retrofit into USAF engines.

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APPENDIX C

FUEL/ENGINE/AIRFRAME TRADE-OFF STUDY
(AIRFRAME STUDY)

September 21, 1979

By

J. E. Drever
J. J. Snider

MCDONNELL DOUGLAS CORPORATION

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INTRODUCTION

In May 1978, Douglas Aircraft Company received a contract (USAF Contract 33615-78) from the Air Force to study the effects of broadening the specifications for JP-4 and JP-8 fuel on the performance and cost of all USAF aircraft presently using JP-4 as well as those expected to be introduced into the force structure by 1983.

Phase I of this study was to determine analytically the effects of these specification changes on minimizing fuel cost and maximizing the fuel availability/flexibility without degrading performance, safety, and survivability/vulnerability.

The following property changes were to be considered:

Grade JP-4:	Freeze Point	+ 14°F
	Final Boiling Point	+ 25°F
Grade JP-8	Freeze Point	+ 18°F
	Final Boiling Point	+ 25°F
	Smoke Point	- 2 mm

Union Oil Company was chosen to study the property variation effects on fuels, Pratt & Whitney Aircraft Group studied the effects on engines, and McDonnell Douglas studied the effects on the airframe.

First, it was necessary to determine a realistic minimum ambient air temperature envelope that the study airplanes would be subjected to. The MIL-STD-210B one-day-per year risk minimum temperature was used for this purpose.

It was beyond the scope of this program to study all of the airplanes in the Air Force inventory, therefore several of the "high fuel user" airplanes were selected.

The fuel systems and fuel management methods of the McDonnell Douglas airplanes were studied in detail to evaluate the effect on system performance of operating with tank fuel temperature near the freeze point.

The fuel systems and fuel management methods of the other manufacturer's airplanes were then studied in somewhat less detail.

AMBIENT TEMPERATURE LIMITS

Available data from Douglas records, MIL-STD-210B, IATA, NASA, and ASTM was reviewed to obtain a minimum temperature vs. altitude envelope for use in establishing the lower limits of ambient temperature for all of the airplanes chosen for this study. This data, shown in Figure 1, indicates a static air temperature of -74.5°C at an altitude of 42,000 feet for the MIL-STD-210B one-day-per year risk minimum temperature plot. Although this appears to be a very low temperature, the scarcity of the data does not allow for the assumption of a higher level of temperature. Therefore, it was concluded that the MIL-STD-210B one-day-per year risk minimum temperature profile (Figure 2) represents a fair and reasonable minimum ambient temperature envelope for USAF aircraft operations when compared to the other data points on the graph.

An analysis was performed which placed each of the airplanes in this study within this temperature environment during three representative conditions. The three flight conditions were: a heavy weight maximum range cruise, a light weight maximum range cruise, and a light weight maximum endurance cruise. The ram recovery temperature for each airplane at several altitudes was calculated using a recovery factor of 0.9.

These recovery temperatures, also known as the adiabatic wall temperature, represent the lowest temperature that the fuel in the tanks could possibly reach. It is the equilibrium condition when the fuel temperature equals the aircraft tank surface (wing skin) temperature. Flight tests have shown that the fuel temperature starts to "approach" the recovery temperature in approximately four hours for outer (thin) integral wing tanks.

The results of this analysis are referred to under the discussion of each airplane later on in this report. These results show that the recovery temperature for most of the airplanes and for most of the flight conditions will be below the present specification maximum freeze point temperature limit of JP-8. For some airplanes and flight conditions the recovery temperature is below the present specification limit for JP-4. These "limiting condition" recovery temperatures provide a good reference level to evaluate the impact of raising the fuel freeze point temperatures for the various aircraft under study.

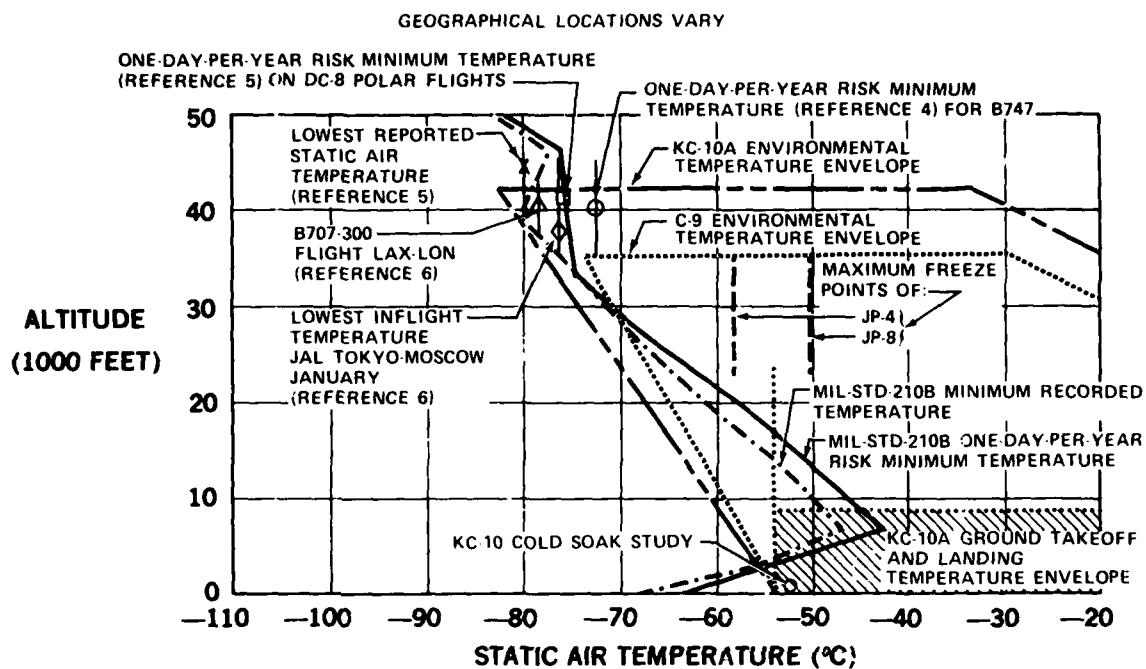


FIGURE 1. MINIMUM STATIC AIR TEMPERATURE STUDY

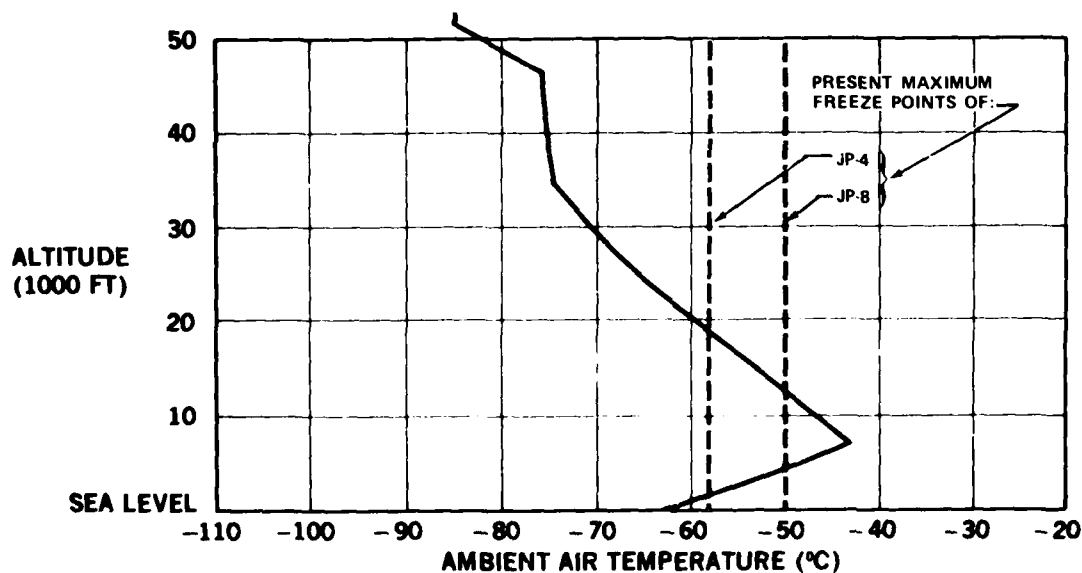


FIGURE 2. MIL-STD-210B ONE-DAY-PER-YEAR RISK MINIMUM TEMPERATURE

FUEL PUMPABILITY TESTS

NOTE: The following material was taken from a previous Douglas report, now out of print (Ref. 1). It is repeated here because the discussion of tests and the test results will provide the reader with some insight into the matter of fuel tank operations at low temperatures.

Several years ago Douglas conducted a series of tests to study the behaviour of kerosene (with properties similar to JP-8) at and below its pour point and to explore system problems associated with such a fuel. The Shell Oil Company furnished 2 fuels for use in this investigation, one with a high freeze point (-35°F , kerosene No. 1) and the other with a low freeze point (-62°F , kerosene No. 2). Figure 3 shows the variation of viscosity with temperature for these fuels. The low freeze point kerosene had slightly higher viscosity than the other, both being above 10 centistokes at -30°F . Both submerged fuel booster pumps and line mounted booster pumps with remote suction were used in these tests, however, no significant differences were observed. Although the cooling rate in the test tank was not necessarily representative of the airplane, a study of the manner in which a kerosene cools and solidifies is very interesting. Figure 4 shows time-temperature histories of several thermocouples located at various distances from the bottom skin of the tank.

Note the steep temperature gradient that exists vertically in the fuel. At the time that the inner surface of the tank bottom reaches the pour point of the fuel, a layer of solidified fuel begins to build up. This in turn tends to act as insulation, reducing the rate of cooling of the bulk of the fuel. This effect can be noted in Figure 4, and appears to begin sharply coincidental with the inner skin surface reaching the fuel pour point. Note particularly the thermocouples located 1 and 2 inches from the bottom of the tank. Although Figure 4 is representative of the manner in which kerosene would cool and solidify in an aircraft tank, the time involved would differ. It was found that the unsolidified fuel could be readily pumped from the tank, or drawn from the tank by a line mounted pump, even though the suction point was deeply immersed in the solidified portion of the fuel. The portion of the fuel, however, which was at or below its pour point remained in the tank as a layer along the bottom, sides, and, to a lesser degree, along vertical structure. It was concluded that the pumping system could easily handle any and all of the fuel that would flow to the pickup point. As would be expected, the power required to pump the fuel increased to a marked degree as the viscosity increased. Figure 5 shows the increase in power required to pump the low freeze point kerosene as it cooled to its pour point.

The next question which arose was, "What if the fuel in crossfeed and transfer lines becomes solidified? Is the system blocked?" This was investigated quite extensively and Figure 6 shows typical results. The pump discharge pressure required to initiate flow in a given system appears to correlate well with how far below the pour point the fuel has cooled. The number of fuel samples tested were not sufficient to conclude that the data presented in Figure 6 is applicable to all fuels, however, the trends are felt to be representative.

Throughout this phase of the testing no difficulty was encountered with plugging of course mesh pump inlet screens (10 mesh), however, it became readily apparent that finer mesh screens or filters, plug solidly with wax crystals if the fuel temperature drops below the freezing point (usually a few degrees warmer than the pour point).

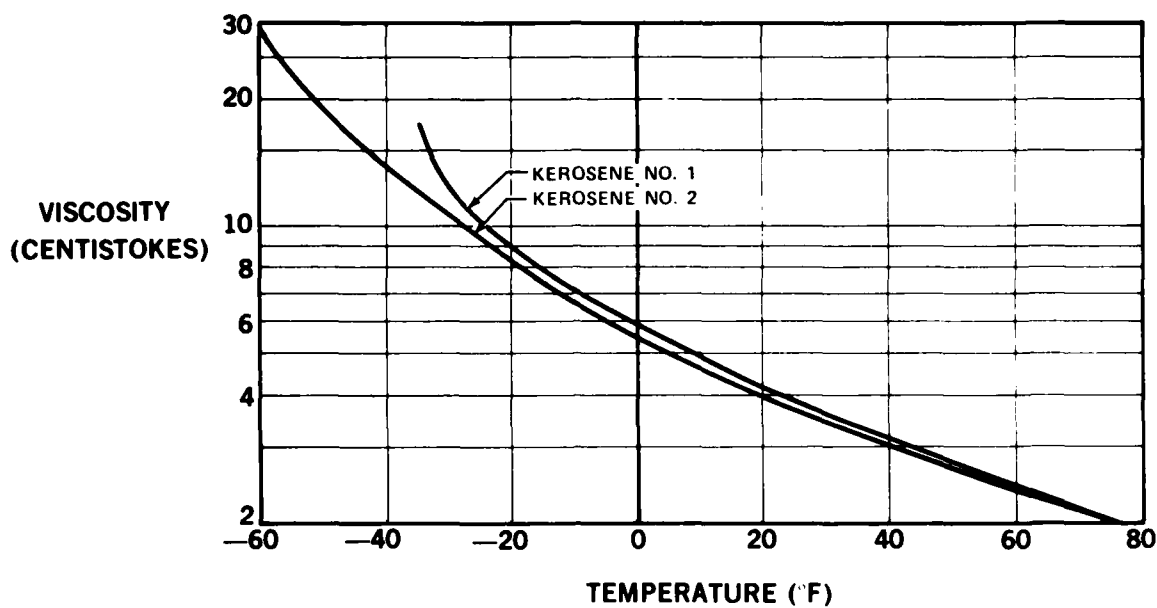


FIGURE 3. VISCOSITY OF TEST KEROSENES

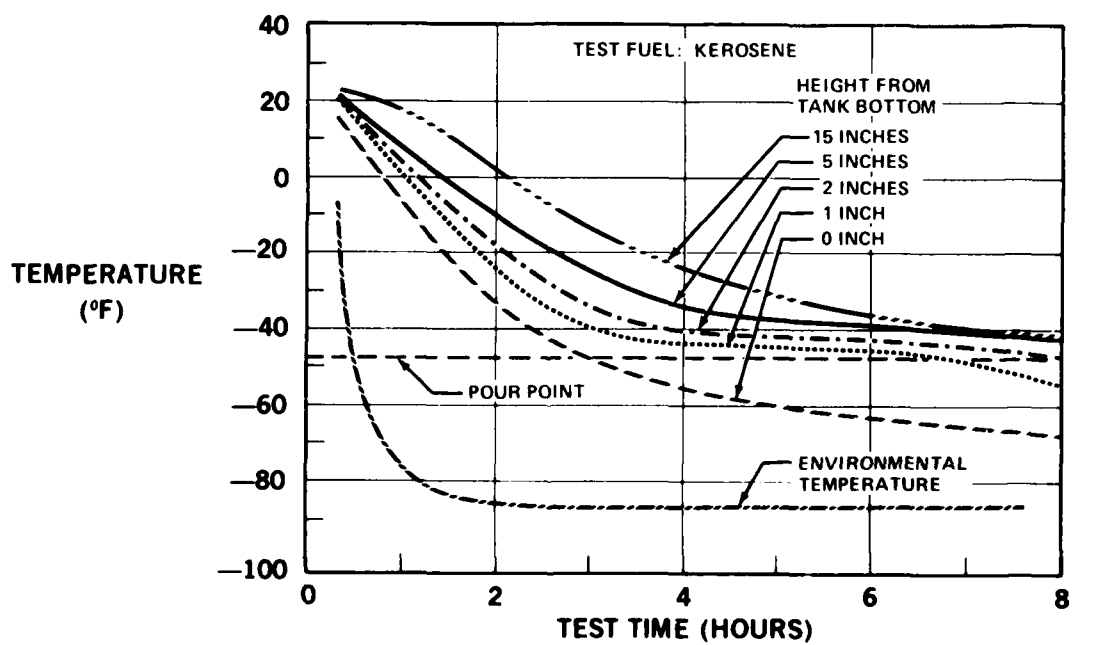


FIGURE 4. FUEL COOLING

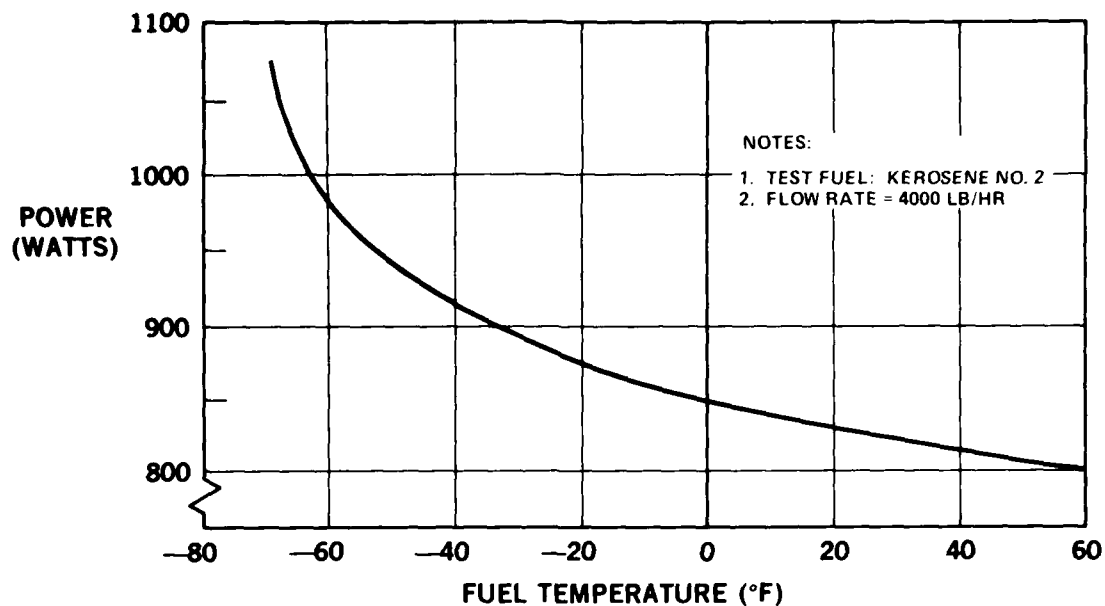


FIGURE 5. POWER REQUIREMENTS OF THE DC-8 TANK-MOUNTED BOOST PUMP

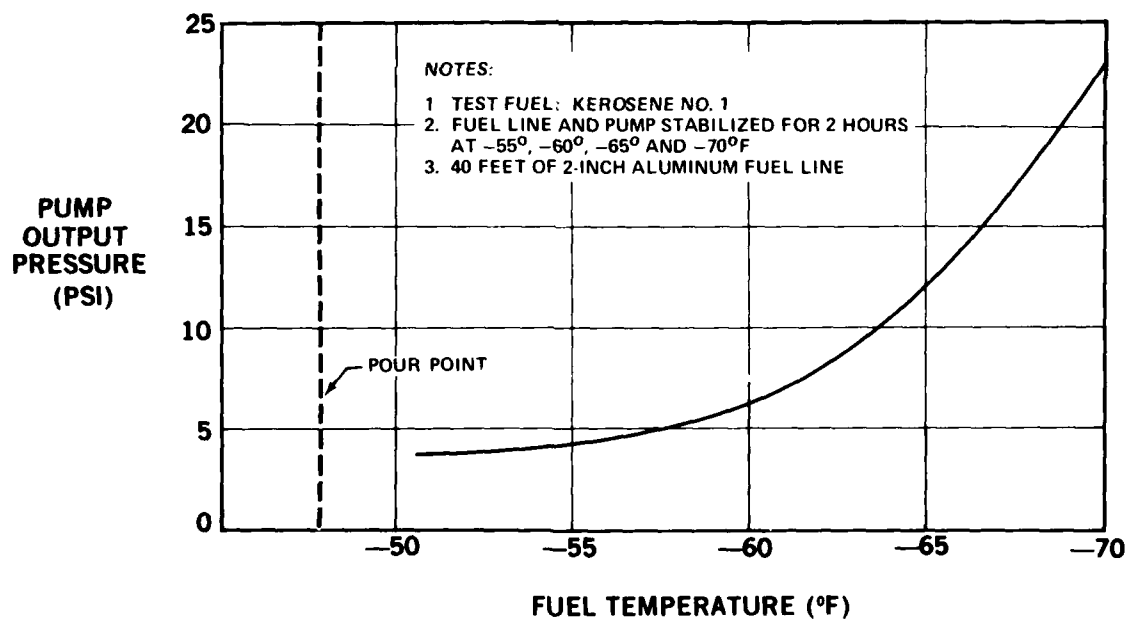


FIGURE 6. PUMP PRESSURE REQUIRED TO INITIATE FLOW OF FROZEN FUEL

Very briefly the major conclusions drawn from this phase of the testing were:

1. When the temperature of the inner skin of a fuel tank reaches the pour point of the fuel contained, the amount of unusable or untransferable fuel in the tank begins to increase.
2. That portion of the fuel contained in a fuel tank which is above its pour point (and would flow to the pickup point) can be readily transferred from the tank.
3. The fuel tank transfer and boost pumps are capable of breaking loose crossfeed or transfer lines if they should become solidified.
4. No fine mesh screen or filter should be so installed that the fuel entering it could be below the fuel's freezing point.

FUEL USAGE REVIEW

The purpose of this portion of the study was to determine which airplanes in the Air Force inventory are using and will be using the majority of the fuel supplies from 1979 through 1985. This projection required the use of the estimated utilization (flight hours per year) and the estimated average fuel usage rate (gallons per hour) for all of the aircraft in the Air Force inventory. The product of the two results in total fuel usage (gallons per year). This information was obtained from the Air Force planning report of Reference 3. To be practical, this study had to be limited to ten airplanes and could not consider such factors as geographic variations of the aircraft bases, or the types of missions to which the aircraft are assigned.

The ten highest fuel users for each year of the study are shown in descending order on the bar graph of Figure 7. Besides the McDonnell Douglas KC-10A, C-9, F-4 and F-15, the five other airplanes chosen for further study were the B-52, C-130, KC-135, C-141, and C-5. Examination of these aircraft provides the best answer for the USAF within the scope of this study. Only a few aircraft have to be considered to provide information on potential fuel cost and availability effects sufficient to indicate courses of action. The chart shows that these airplanes represent aircraft which consume 75 percent of the projected fuel used through fiscal year 1985. The F15 joins the group of airplanes which consume 75% of the fuel used by the USAF in the year 1981. The shaded area on the graph shows the amount of fuel used overseas as a percentage of the 1979 total fuel used. Overseas fuel usage is not a significant percentage of fuel used for the purposes of this study.

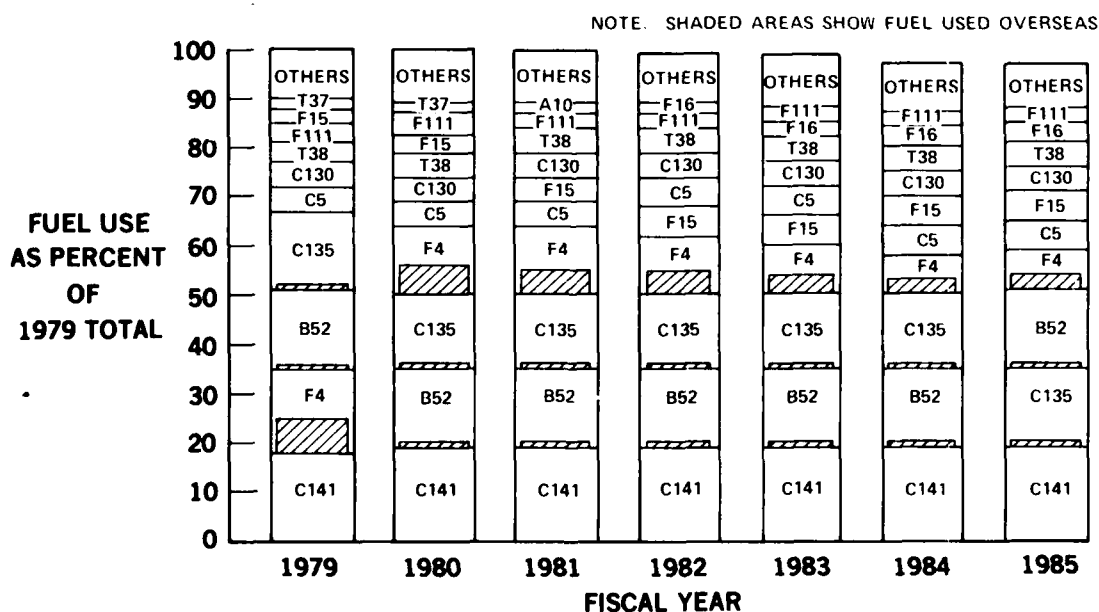


FIGURE 7. PROJECTED FUEL USE

M.D.C. AIRPLANES AND SYSTEMS

KC-10A

The tankage system consists of three main tanks, a center wing auxiliary tank and two fuselage tanks. The mains and center wing tank are of the integral type and the fuselage tanks are of the bladder type. A schematic drawing of the tankage system is shown in Figure 8. The engines are normally fed from their respective main tank. Usually, fuel to satisfy the KC-10A fuel burn and reserve requirements will be carried in the mains and center wing. Fuel in the fuselage tanks will be used for off-loading to aircraft being refueled in flight. Fuel may be carried in the fuselage tanks to extend the range of the KC-10A.

During normal fuel management where center wing fuel is carried, the mains are continually being "topped off" by the transfer of fuel from the center wing tank. When the center wing tank is depleted, fuel is transferred to the Numbers 1 and 3 Main from the Number 2 Main. When the Number 2 Main quantity becomes equal to the Numbers 1 and 3 Main, all three main tanks are then depleted evenly for the remainder of the flight. The Number 1 and 3 Main Tanks are each divided into inboard and outboard (tip) compartments. The tip fuel (5300 pounds) is retained until each main tank is down to approximately 10,300 pounds of fuel (5,000 pounds in the inboard compartments). This tip fuel will be the coldest fuel on the airplane because it is used last on a normal mission and it is held after transfer from the other tanks stops in a compartment with a large surface area to fuel volume ratio. This scheme of fuel management provides for relief of aerodynamically induced wing bending moments and provides flutter damping resulting in significant savings in aircraft weight and fuel burned.

The tip compartment fuel is not isolated for the entire duration of the flight because the Number 1 and 3 Main Tank fill valves are located in the tip compartments. The warmer fuel from the center wing tank (and fuselage tanks if it is transferred to the mains) is continually passed through the tip compartments during the transfer process. The normal fuel management schedule is automatically controlled by fuel system design features for the most part and requires only minimal input and monitoring by the flight crew.

The fuselage and center wing tanks contain the hydraulic powered aerial refueling pumps for fuel off-loading. The fuel in these off-loading tanks can be transferred to the mains for engine usage if necessary. Also, the fuel in the mains can be transferred to the off-load tanks. The fuel in the off-load tanks is insulated, due to the tank locations within the fuselage, and will remain much warmer than the fuel in the mains for the entire flight. Very cold fuel transferred from the outboard mains to the fuselage tanks for off-load will be mixed with the much warmer fuel remaining in the off-load tanks and will not be transferred directly to a receiver aircraft.

DC-10 Flight test data shows that the tip tank bulk fuel temperature approaches the adiabatic wall temperature after about four hours of flight but will vary significantly with the fuel loading on the airplane due to transfer of warmer fuel through the tip compartment. A while after transfer has stopped, test data on one flight show the No. 1 and 3 main inboard compartment boost pump inlet temperature is approximately 3 to 5°C warmer than the tip tank bulk temperature. The No. 2 main tank fuel temperature is approximately 11 to 14°C warmer. At this time, the temperature of the fuel carried in the center wing tank, but not used on this flight was 28 to 32°C warmer than the tip fuel.

SIMPLIFIED SYSTEM SCHEMATIC

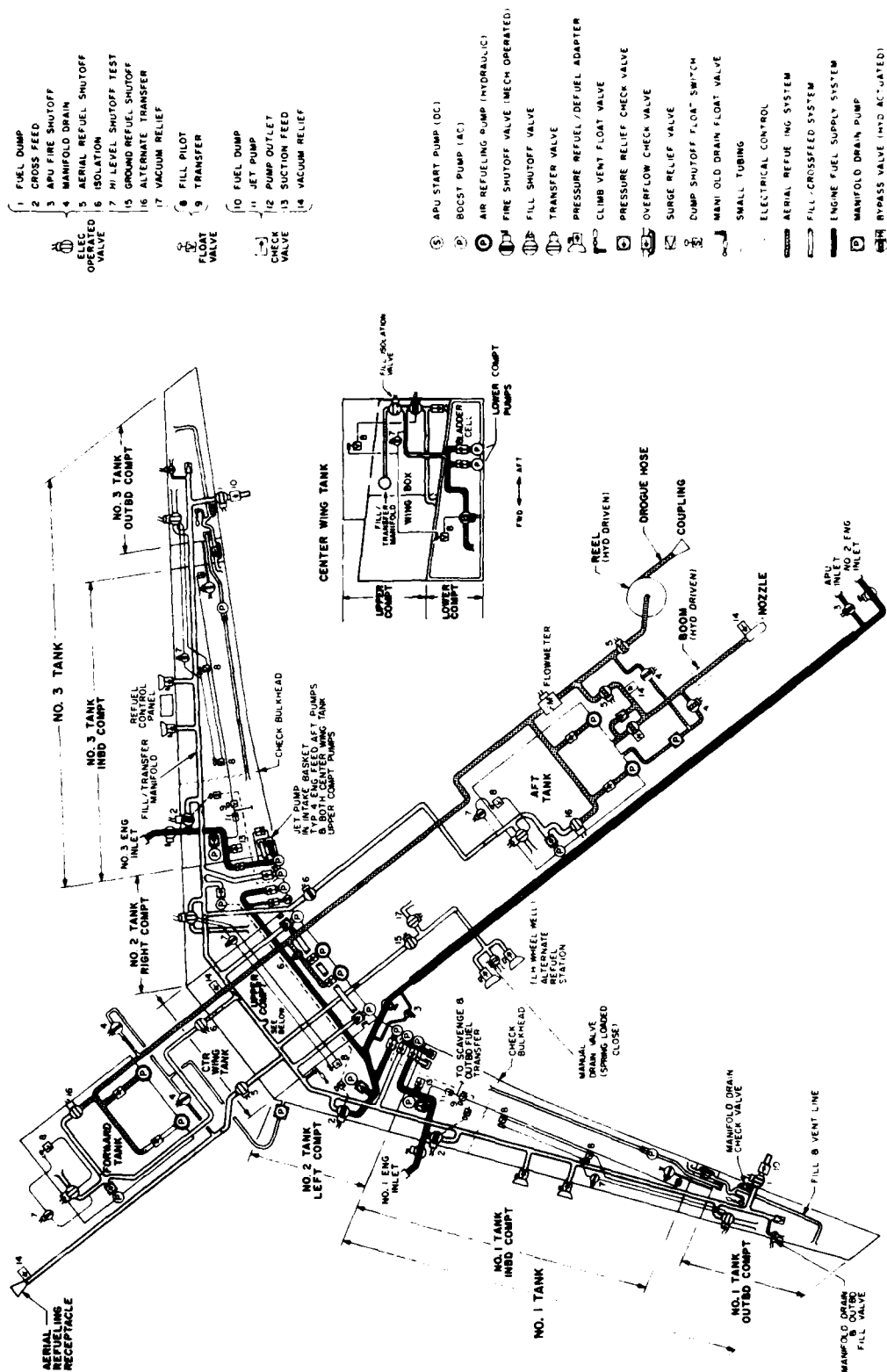


FIGURE 8. KC-10A FUEL SYSTEM

The tank fuel temperature is measured in the No. 3 main tank tip compartment and displayed in the cockpit. The flight crew, therefore, has an indication of the coldest fuel on board the airplane. The flight manual instructions say to vary the flight plan to warm the fuel if the temperature cannot be held to at least 30°C above the freeze point of the fuel on board.

There are three different systems for transferring the tip fuel to the inboard compartment. The primary system is the gravity drain system. It consists of a drain pipe positioned near the bottom tank skin and a gravity transfer valve. The valve is held closed by the inboard boost pump pressure. This gravity transfer valve also passes about 4 gallons per minute (gpm) of inboard fuel through the drain pipe to the tip compartment to make up for fuel taken from the tip compartment by the water scavenge system. When the inboard float lowers to approximately 5000 pounds, the pressure supply is shut off and the tip tank fuel head opens the valve. The inboard flow rate is approximately 15 gpm. The operation of the gravity transfer system is questionable when the fuel temperature is near the pour point. The 4 gpm of relatively warmer "make up" fuel should keep the upper portion of the drain pipe and the valve thawed out and allow it to actuate open, however, the inboard flow will be reduced below the nominal 15 gpm.

The second system for tip tank transfer is the jet pump system designed for operation at nose high attitudes when the gravity system cannot operate. The jet pump suction line which is 55 inches long and 2 inches in diameter could become full of congealed fuel. As an example, assuming a case where the fuel in the line is an arbitrary 40°C below the pour point, the pressure required to push the slug of fuel out can be calculated to be approximately 0.5 pounds per square inch (psi) by use of Figure 6. This pressure is within the jet pump performance envelope if the fuel in the suction line is not too viscous to prevent necessary mixing action within the jet pump mixing chamber.

The third system for tip tank transfer is the electric tip tank transfer pump system, designed to transfer the fuel inboard rapidly for the case of a large off-load mission. The pump is located in the inboard compartment with a 2 inch diameter, 410 inch long suction line to the tip compartment. The suction pressure required to remove a slug of congealed fuel at 40°C below the pour can be calculated to be approximately 4 psi. It is doubtful if the boost pump could provide this much suction pressure.

The purpose of the above discussion was to determine if it was feasible to allow the fuel freeze point to be raised to a value that would result in a layer of congealed fuel on the bottom of the tip tank. It appears that the possibility of obtaining timely fuel flow from the tip tank to the inboard compartment and thus to the engines would be questionable with a large amount of congealed fuel on the bottom of the tank. Also, if this were allowed, there could be a point in time, later, when the warmer fuel in the No. 1 and 3 inboard compartments (where the boost pumps are) would also have this build-up of congealed fuel. This buildup would be deeper at the rib locations due to the greater heat sink. This could seriously affect outboard to inboard flow (to the boost pumps) at these locations. Therefore, to insure reliable fuel flow to the engines, no significant buildup of congealed fuel should be allowed on the bottom surface of any of the tanks. The worst case limit cannot be defined at this time for any particular airplane beyond the limits currently defined by the manufacturers.

The above discussed systems are the systems that are considered most likely to be adversely affected by fuel temperatures near the freeze point. Other systems considered were the primary transfer, off-load, quantity indicating, engine feed, auxiliary power unit (APU) feed, fill, defuel, jettison, scavenge, and vent systems. These systems are considered in the following paragraphs. Systems and functions are described and operation at low temperature is discussed.

The primary transfer system transfers fuel from the center wing tank and No. 2 main tank to the No. 1 and 3 mains by means of electric powered transfer pumps, and the normal fill valves. The fuselage tanks also contain electric powered transfer pumps for inter-tank transfer. These transfer systems should present no difficulties since the fuel tanks involved are in a relatively insulated location. The fuel being transferred should not be near the freeze point, therefore the No. 1 and 3 main fill valves (located in a cold area), high level control valves, and sense lines should perform normally.

The off-load system consisting of the large hydraulic off-load pumps, piping, and the fueling boom and hose reel assemblies should present no problems. The hose reel is located in the cabin environment. With the fueling boom in the stowed position, there is about 9 gallons of trapped fuel at the boom elbow. This fuel will approach the recovery temperature in about 30 minutes. However, even if the temperature of this fuel was a substantial amount below its pour point, the hydraulic transfer pumps would easily push the congealed quantity out through the boom. This slug would be further broken up when it passes through the nozzle-receptical and receiver aircraft piping. It would also absorb heat in the receiver aircraft and would liquify.

The fuel quantity indicating system could give erroneous readings if congealed fuel formed on the bottom of the tanks to a depth sufficient to plug the lower end of the quantity probes. If this happened, the fuel depth in the tank could be below that within the probe. However, since the probes are mounted at an angle with respect to the tank bottom, and the lower end of the probes are several inches from the stringer locations (where the congealed layer is thicker), the probe ends should not become blocked. A study was conducted to determine if the aromatic content of the broadened specification fuel could have sufficient effect on the fuel dielectric constant to affect the accuracy of the fuel gauging system. It was found that this effect was negligible. As an example, a change in aromatic content of 25% (much greater than would occur under the proposed specification variation) would produce about the same change in dielectric constant as that which would result from a 50°C change in temperature (approximately 5%).

The fuel feed system will function properly, once the fuel in the tank flows to the boost pump inlet or to the pump inlet pipe. The pumps have a positive displacement reprime element that creates a strong vacuum at the pump inlet which is sufficient to clear the inlet lines. When a pump is started after soaking, previous testing indicates that fuel pump pressure will clear lines of fuel after soaking at temperatures well below the pour point.

The main problem to be expected with the APU fuel feed system would be during ground operation after cold soaks at extreme temperatures where fuel was congealed. The direct current powered APU fuel system start pump was not designed to operate with congealed fuel and therefore there would be a question as to its suction capability for drawing fuel through its inlet line. Depending on the condition of the pump and the available fuel head in the fuel tank, the minimum

required APU fuel inlet pressure may not be maintained, and starting and running problems may be experienced. However, no problems have been reported in commercial service.

The fuel fill system including the fill valves, high level control valves, and sense lines should operate normally because the ground fuel supply temperature should be above the freeze point. There should be no effect on fill rates or fill times since the density and viscosity will remain within the present specification limits. A case to be considered is the case of an airplane fueled with JP-8 landing at a base with extremely cold ground temperatures. Problems may be encountered if the airplane was allowed to "cold soak", and then it was desired to defuel without the use of electrical power by means of suction defueling, in order to refuel with JP-4. If the fuel in some of the tanks or some portion of the tanks starts to congeal, this may lead to a flow rate unbalance between tanks. This could result in one of the tanks being depleted of fuel or its suction line to unport while the remainder of the tanks still contain fuel, preventing any further defueling by this method.

The jettison system should present no difficulties. By the time that the fuel becomes cold enough to cause any problems, there should be no need to jettison fuel. However, the valve is located somewhat behind the aft spar and could possibly approach the recovery temperature. This valve opened satisfactorily during a test while filled with water and frozen solid. It should therefore operate normally if filled with fuel below the pour point, and the transfer and boost pumps will push the slug of congealed fuel out through the valve.

The scavenge system consists of jet pumps, scavenge rakes, and associated valves and plumbing for the purpose of scavenging the water (and fuel) from the low points in the tanks and feeding it to the engine boost pumps. This system operates on very low pressures, incorporates small line sizes, and will no doubt fail to function if the fuel temperature at the lower surface of the tank is in the vicinity of the pour point. This presents no problem, however, the system is not essential for flight and may become inoperative for the limited amount of time that the fuel is this cold.

The vent pipes are routed in the center of the tank, away from the coldest fuel and should present no problems. The vent exit on the bottom of the wing surface contains a flame arrestor to prevent the propagation of a flame under the lower surface of the wing into the vent system. If this arrestor becomes blocked with ice, the fuel tanks are vented through by-pass valves. The vent exit standpipe, flame arrestor, and by-pass valves are located within a vent box. It would take a very thick layer of congealed fuel on the bottom of the vent box to affect the operation of the climb by-pass valve. However, there should be no congealed fuel in the vent box to hinder proper operation of the climb by-pass valve because the fuel won't be cold enough during this portion of the flight (climb after take-off). The descent by-pass valve should operate normally since it opens inward away from the congealed fuel.

Figure 9 shows the KC-10A recovery temperature calculated for three different flight conditions while operating in the MIL-STD 210B one-day-per year risk minimum temperature environment. It shows a minimum temperature of -60.5°C between 30,000 and 35,000 feet for a light weight maximum endurance condition. This represents a very severe case since the mission would have to be a holding pattern within a very cold air mass while the airplane gross weight is very low. However,

it is not unrealistic if the tanker was for some reason required to hold locally for a considerable period after off loading most of its fuel. An example would be to refuel a group of fighters whose refuel volume requirements would be very low, but for whom refueling would be essential for their safe return to base. The curve on the right of Figure 9 shows the highest temperature to be expected (-49.5°C) for a heavy weight KC10 flying at maximum range speed in the same environment. This condition is a little unrealistic since a maximum range mission would not remain for a long period of time in a locally cold air mass. However, Figure 9 does show the minimum recovery temperatures that could possibly exist.

A heat transfer computer program was used to calculate the KC10 tip tank fuel temperature considering the effect of time. This approach is more realistic than simply assuming that the fuel reaches recovery temperature. The effective overall heat transfer coefficient and effective tip tank surface area were known from existing DC10 flight test data. The minimum tank fuel temperature was predicted to occur during a KC-10A tanker mission flying a holding pattern at 35,000 feet at a maximum endurance power setting. The ambient temperature was a constant -74.5°C from the Mil 210B one day per year risk minimum temperature envelope. Figure 10 is the resulting plot of the No. 1 or 3 main tip compartment (critical tank) bulk fuel temperature vs. flight time. The curve shows that the fuel temperature reaches the recovery temperature of -54°C late in the flight. The curve was plotted for initial fuel temperatures of 16°C (60°F), -18°C (0°F), and -29°C (-20°F). As shown, the initial fuel temperature has little effect on the final fuel temperature on long range flights.

The cargo or troop transport mission would involve flying through areas of low temperature extremes rather than loitering in these areas as in a tanker rendezvous mission. Therefore, the tank fuel temperatures should not get as low on transport flights. The results of a KC-10A light weight cargo mission are shown in Figure 11. The temperature profile selected was that of the 4900 nautical mile commercial flight as reported in Reference 4 adjusted to MIL-STD-210B temperature levels. The minimum temperature on the profile was -72°C as compared to -74.5°C from MIL-STD-210B shown in Figure 2 of this report. The -72°C was based on a commercial flight route and data from the Naval Weather Service Atlas and is not as stringent a requirement as MIL-STD-210B. Therefore, the temperature profile from the referenced report was adjusted slightly by lowering the entire profile by 2.5°C (-74.5-(-72.0)). The results show that the bulk fuel temperature reaches -48.5°C and does not quite reach the minimum recovery temperature of -50.2°C before the ambient temperature of the route starts to increase.

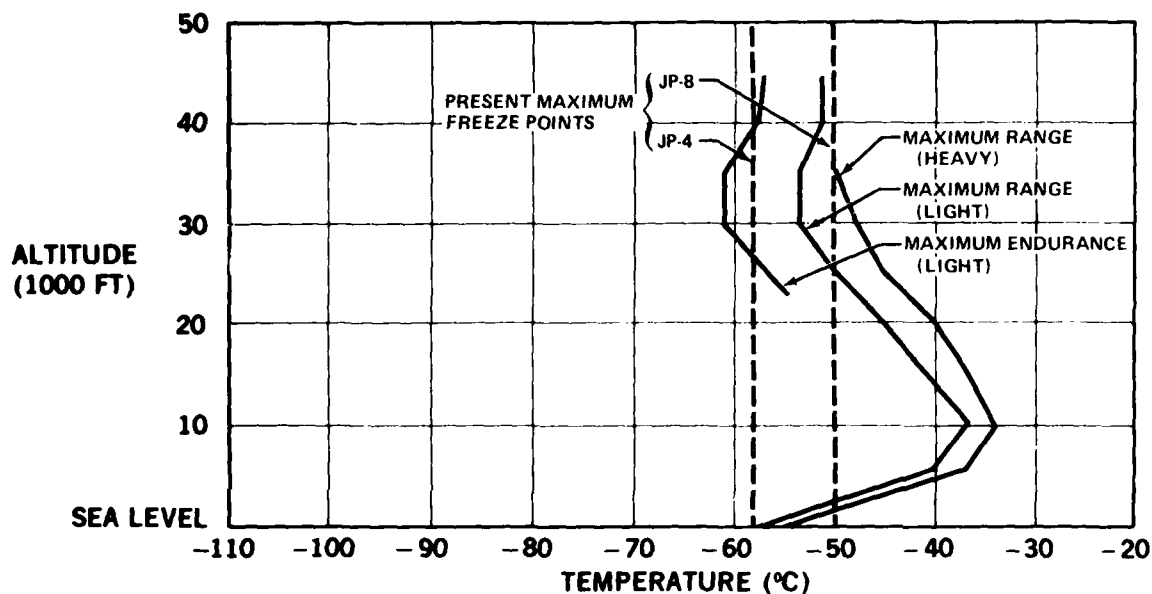


FIGURE 9. KC-10A ADIABATIC WALL TEMPERATURE

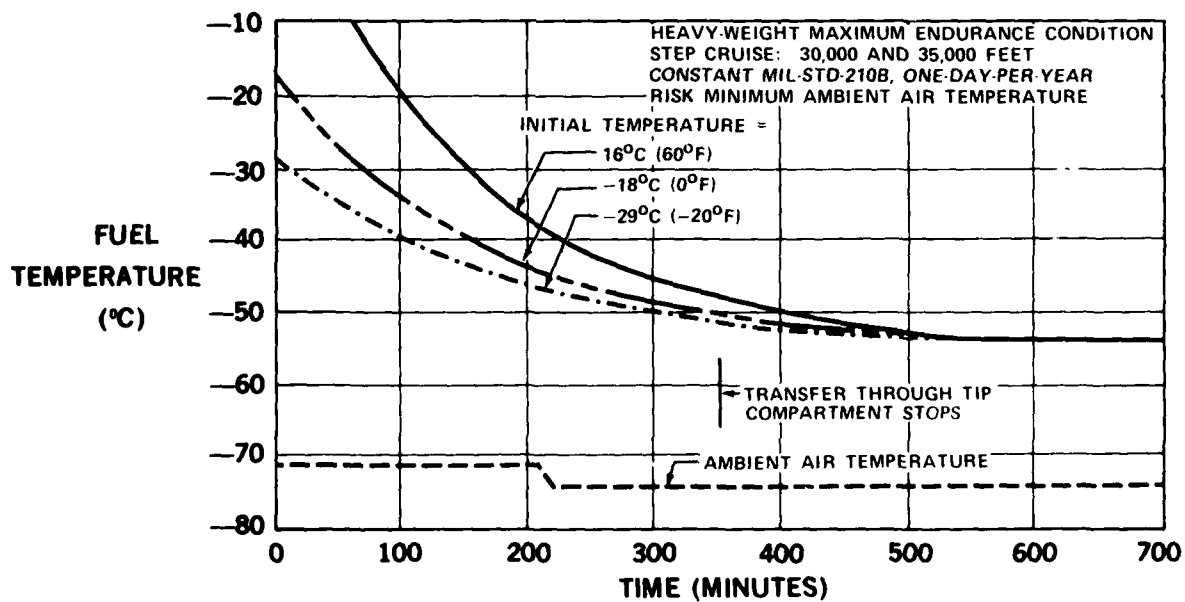
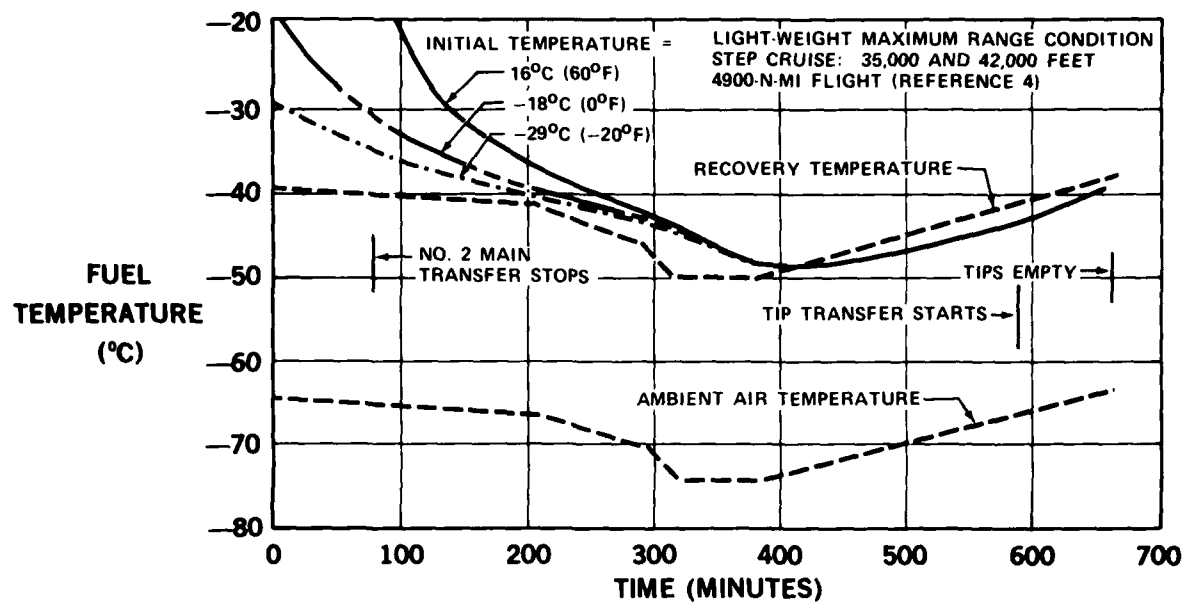


FIGURE 10. KC-10A TIP COMPARTMENT BULK FUEL TEMPERATURE-TIME-HISTORY - TANKER MISSION



9 GEN 28605

FIGURE 11. KC-10A TIP COMPARTMENT BULK FUEL TEMPERATURE-TIME-HISTORY - CARGO MISSION

C-9

The tankage system consists of two main tanks, a center wing auxiliary tank and a forward and aft fuselage auxiliary tank. The two mains and the center wing tank are all integral type and the fuselage tanks are of the bladder type. See Figure 12 for schematic. All auxiliary fuel is used first and is pumped directly to the engines, overriding the lower pressure of the main tank boost pumps which are operating on standby. This fuel usage schedule uses all of the thermally insulated fuel first, and then uses the fuel from the thin wing tanks near the end of the flight. As in the case of the DC-10 tip tank test data, DC-9 flight test data also shows that the main tank bulk fuel temperature approaches the recovery temperature after about four hours of flight. Since the C-9 can cruise for over this period of time on the auxiliary fuel alone the main tank fuel will have sufficient time to reach temperatures near the recovery temperature. There are no compartment divisions within the main tank that require tank internal transfer systems as in the KC-10A No. 1 and 3 mains. However, there are two sets of check bulkheads (closed ribs with flapper check valves at the bottom to prevent outboard flow toward the wing tips) which the fuel must flow inboard through to reach the tank boost pumps. These check bulkheads provide a heat sink that would increase the rate of fuel cooling at this location, resulting in an increased tendency to congeal fuel around the check valves.

The shallow tanks of the C-9 increases the fuel cooling rate and also provides less tank head to force the cold fuel through the obstructions (flapper bulkheads, rib locations, etc). These factors in combination with low wing dihedral angle, a tendency toward colder fuel (due to lower Mach number and type of fuel management), and lower wing vibration (due to aft mounted engines) would appear to contribute to making the C-9 more critical with respect to cold fuel problems than the KC-10A. This should heighten the desire to provide proper fuels for its operation. Tests would be required if an acceptable level of congealed fuel buildup were to be determined.

The C-9 does not incorporate a tank to tank transfer or jettison system. The fill and de-fuel system utilizes electric motor driven valves and electric float switches and should be more reliable with regard to cold fuel problems than the hydraulic valves and switches of the KC-10A. However, suction defuel operation should suffer from the same potential problems as described for the KC-10 (possible flow unbalance between tanks, resulting in unporting an inlet and thus preventing further defueling). The engine feed, fuel quantity, scavenge, and vent systems are all similar to those of the KC-10A and should present no serious problems.

The C-9 cockpit displayed fuel temperature is measured at the engine fuel filter inlet. Tank fuel temperature is not measured directly. The "dash one" technical manual instructs the crew to avoid flying at altitudes where the indicated fuel temperature is lower than 6°C above the maximum specified fuel freeze point of the type of fuel on board.

Figure 13 shows the C-9 recovery temperature calculated for three different flight conditions that are comparable to the KC-10 mission in Figure 9. As in Figure 9, the environment is a constant MIL-STD 210B one-day-per year risk minimum fuel temperature. Figure 13 shows a minimum fuel temperature of -64.5°C at 35,000 feet for a light weight maximum endurance condition. The right hand curve of Figure 13 shows the highest temperatures to be -53°C for a heavy weight C-9 flying at maximum range speed in the same environment. The C-9 flies transport type

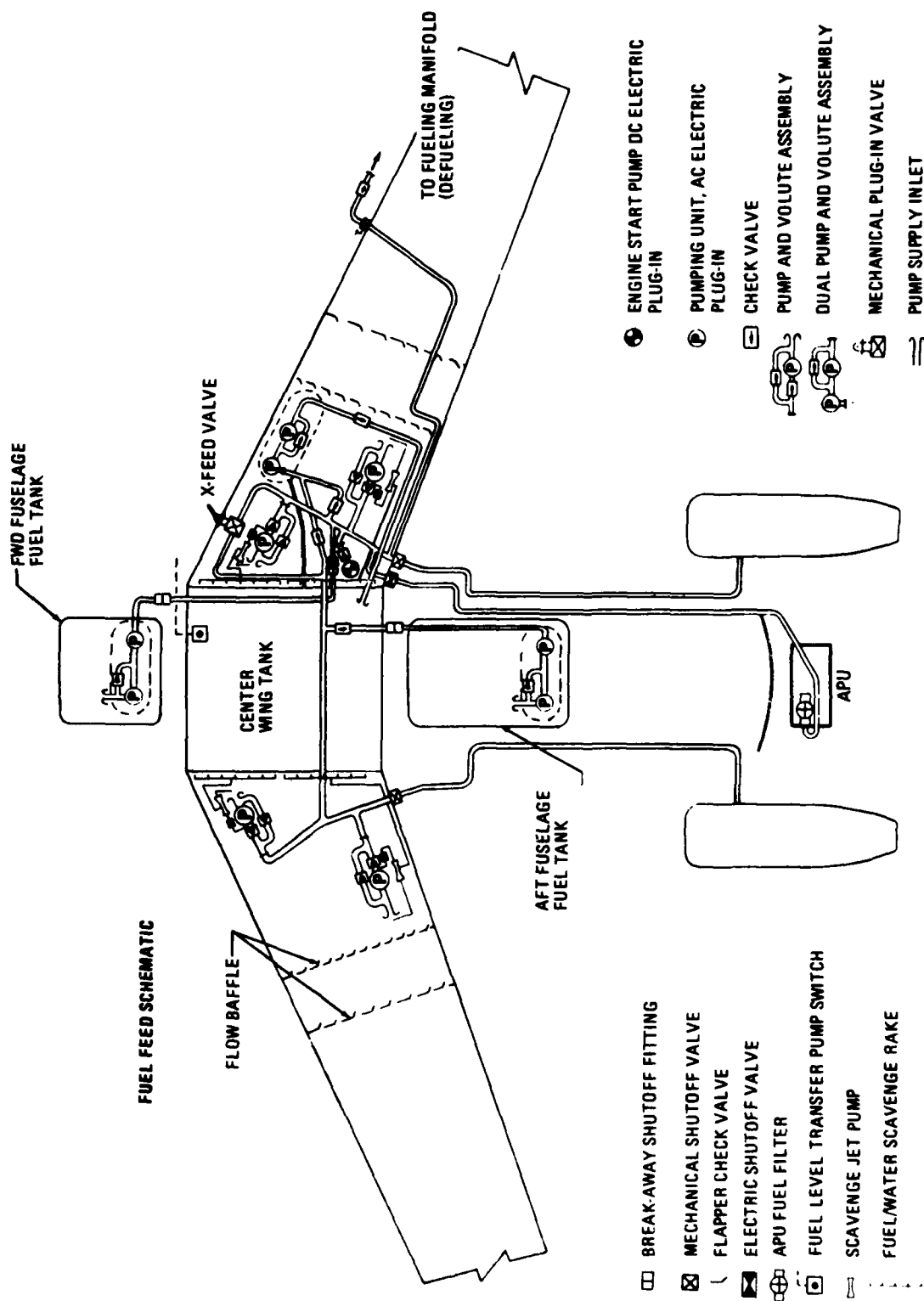


FIGURE 12. C-9 FUEL SYSTEM

missions. It does not fly missions involving loiter/rendezvous missions, so -64.5°C inflight fuel temperatures are unlikely for the C-9.

Figure 14 shows a C-9 main tank time-temperature history on a personnel transport mission as calculated using the heat transfer program. The ambient temperature profile was the 2000 nautical mile flight of Reference 4. The curve shows that the fuel temperature reaches -44°C for all three initial fuel temperatures, but does not quite reach the minimum recovery temperature of -45.4°C .

F-4

Fuel is carried internally in a fuselage tank, made up of six interconnected fuel cells, and in two integral wing tanks. External fuel is carried in three drop tanks. This tankage arrangement is shown in Figure 15. Regulated engine bleed air pressure transfers internal wing fuel and all external fuel to the fuselage tanks. The forward cell of the six fuselage cells is the engine feed cell. Transfer pumps in two of the other five cells, transfer fuel to the feed cell. A schematic of the fuel system is shown in Figure 16.

Normally, the external fuel and wing fuel is used first. The fuselage fuel, which is continually absorbing heat from the engine compartments is used during the last portion of the flight. The short flight times of this type of airplane and its relatively high operational Mach numbers, together with the fuel management schedule, result in fuel temperatures substantially above current fuel freeze points for the entire duration of a flight. Following allowable fuel management schedules there will never be a case where the last fuel used is the coldest fuel aboard the airplane.

Figure 17 shows the integral wing tank recovery temperature calculated for the same three types of flight conditions as was done for the KC-10A and C9 airplanes. It shows a minimum recovery temperature of -57.5°C at 35,000 to 40,000 feet for a light weight maximum endurance condition, and -45.5°C for a heavy weight maximum range condition. This fighter type airplane would not remain in this cold environment long enough for the bulk fuel temperature to reach the recovery temperature. However, even if this was possible, the fuselage tank fuel is the last fuel to be used and would be considerably warmer than the wing tank fuel, thus assuring the flow of fuel to the engine.

F-15

Fuel is carried internally in four interconnected fuselage tanks and two integral wing tanks. External fuel is carried in three 600 gallon external tanks. The internal wing tanks and the forward fuselage tank are transfer tanks. The other three fuselage tanks are engine feed tanks. Figure 18 shows this tankage arrangement. Regulated engine bleed pressure transfers fuel from the external tanks to any internal tank that will accept it and also provides a positive pressure to all internal fuel tanks. Normal fuel transfer is accomplished by three electric transfer pumps and engine bleed pressure. If the electrical transfer pumps fail, all internal fuel will transfer through gravity transfer lines at a reduced rate to the engine feed tanks. A fuel system schematic is shown in Figure 19.

There is an accessory cooling heat exchanger located in each engine fuel line between the fuel tank and the engine fuel pump inlet. The amount of coolant fuel required is greater than the engine fuel flow demand. The coolant fuel that is in excess of the engine demand is returned to its respective integral wing tank.

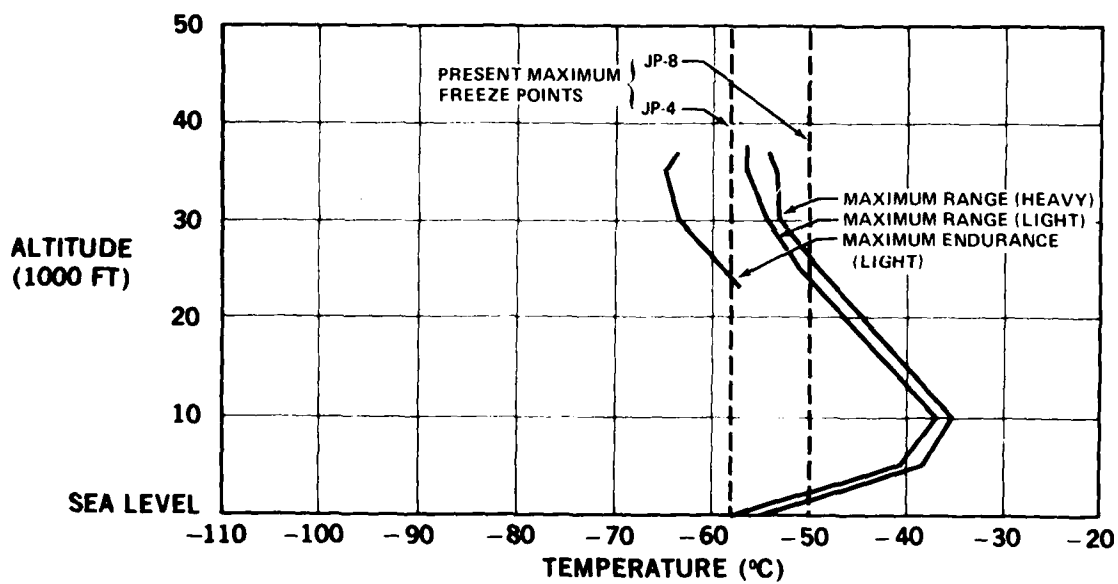


FIGURE 13. C-9 ADIABATIC WALL TEMPERATURE

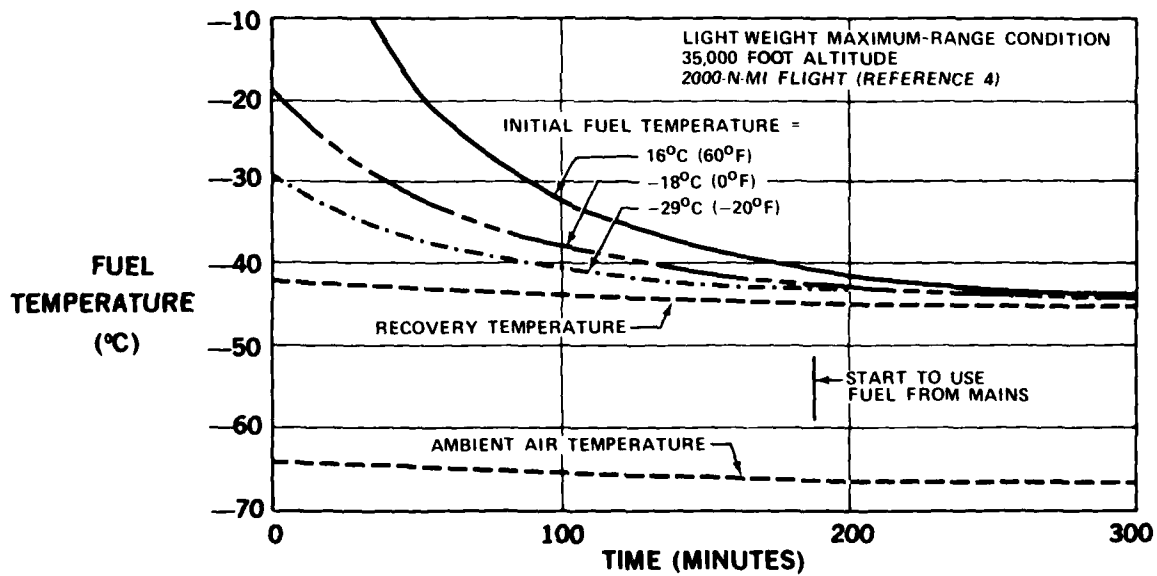


FIGURE 14. C-9 MAIN TANK BULK FUEL TEMPERATURE-TIME HISTORY - PERSONNEL MISSION



FIGURE 15. F-4 TANKAGE ARRANGEMENT

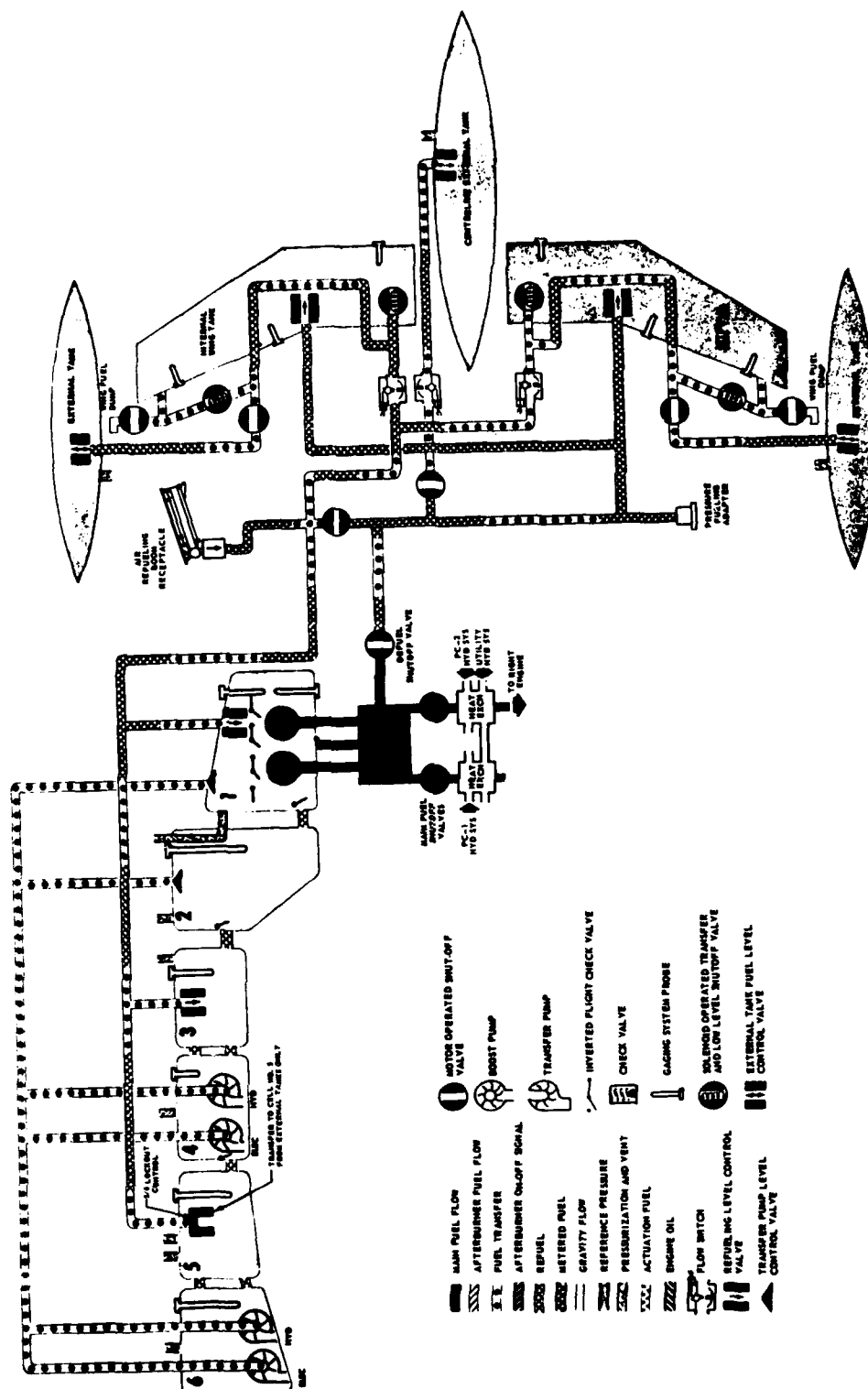


FIGURE 16. F-4 FUEL SYSTEM

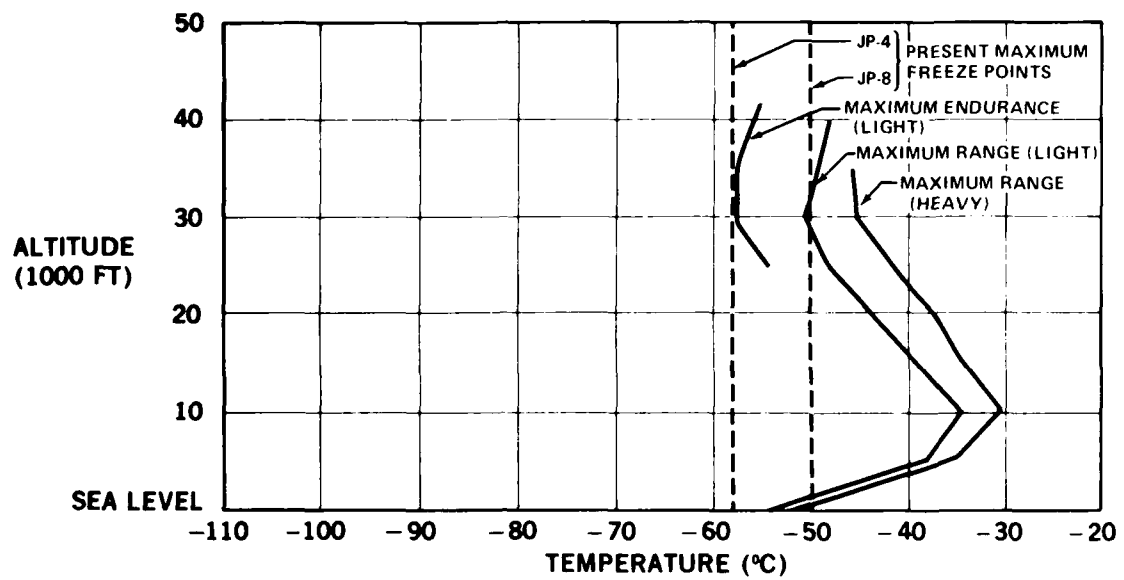


FIGURE 17. F-4 ADIABATIC WALL TEMPERATURE

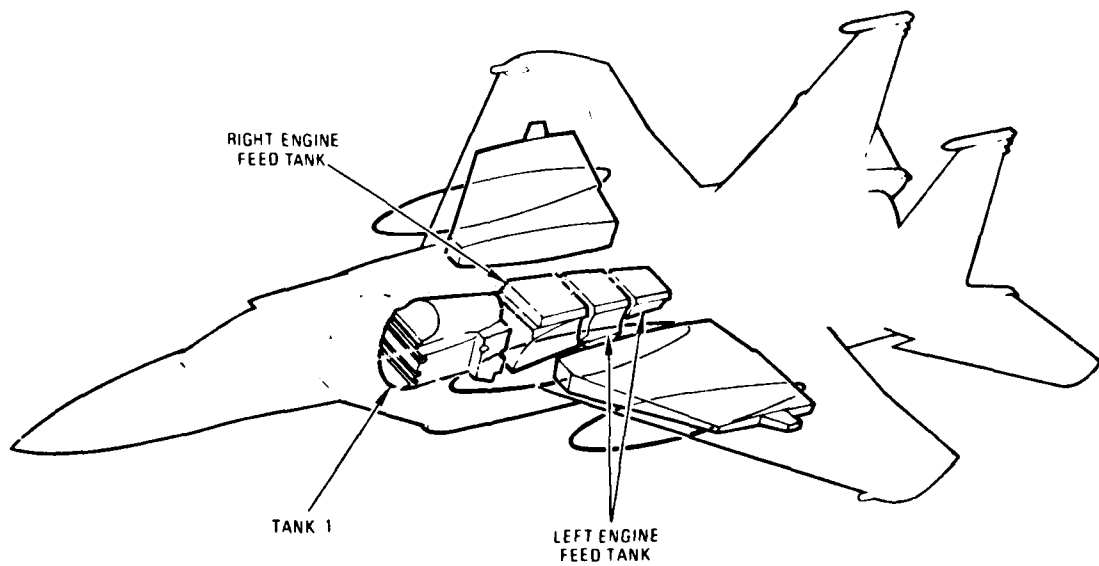


FIGURE 18. F-15 TANKAGE ARRANGEMENT

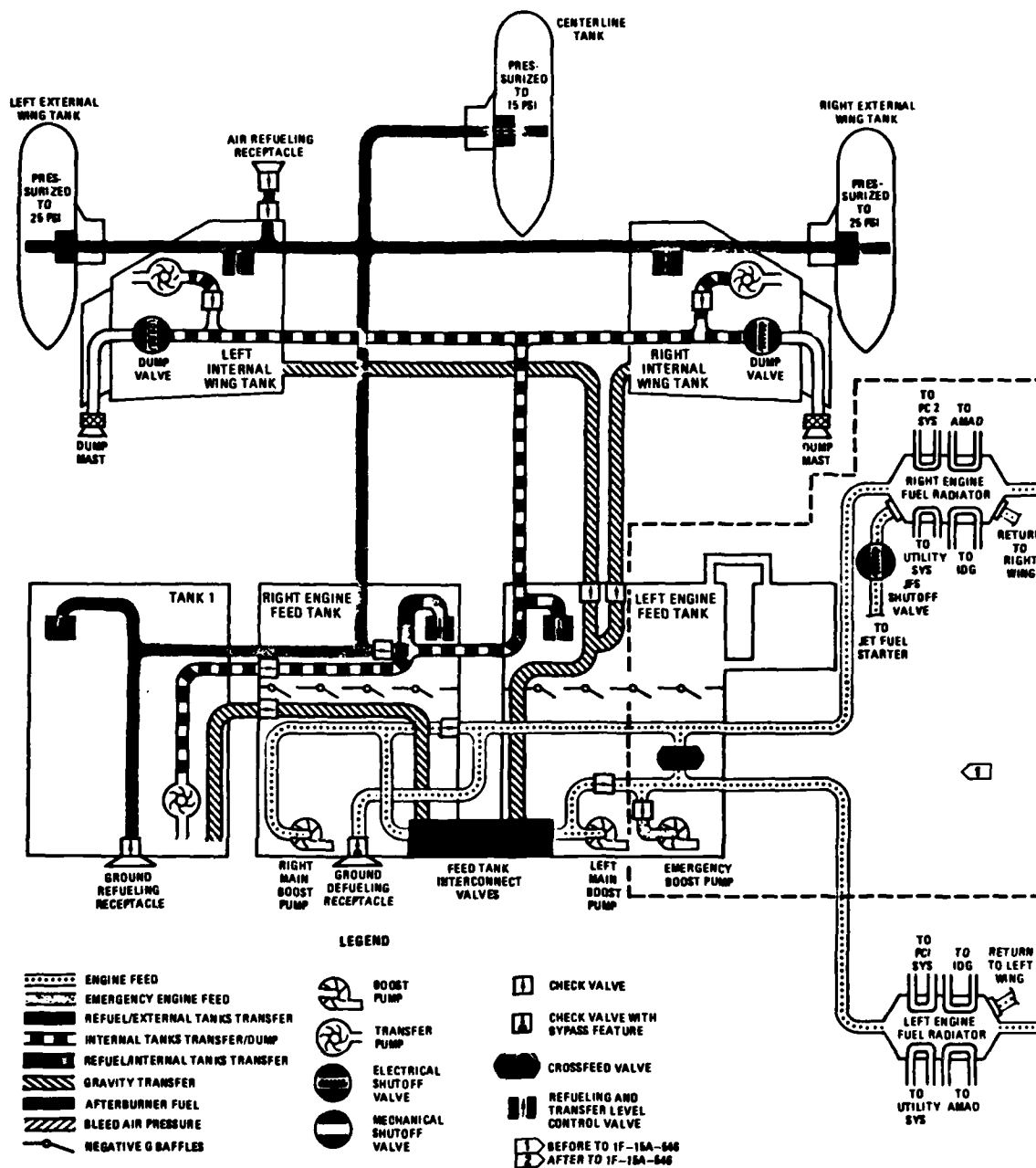
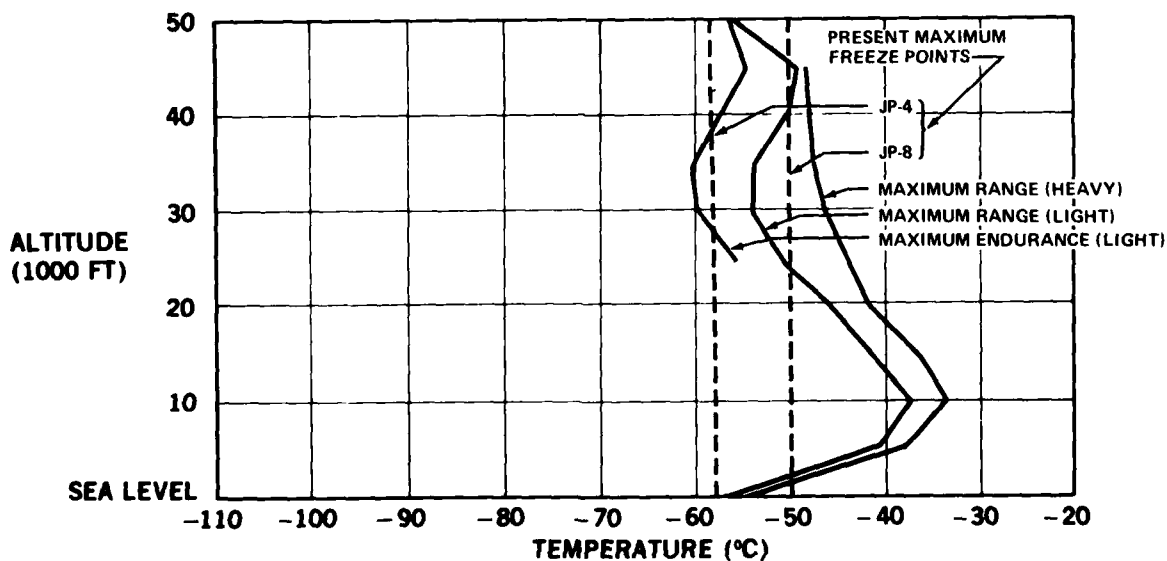


FIGURE 19. F-15 FUEL SYSTEM

This fuel rejects heat to the atmosphere in the wing tank and then returns to the fuelage tanks. In this instance, the heat rejection to the atmosphere is required to prevent the fuel temperature from becoming excessive, quite the opposite of the transport aircraft problem. With the advent of more sophisticated electronic systems which reject more and more heat to the fuel, the problem is one of keeping the fuel cool. Freeze point is not an operational problem in flight, but thermal stability problems (coking in the engine hot fuel sections) may become more of a problem as fuel final boiling points are increased.

Fuel management is essentially the same as for F-4. This fuel system should be relatively free of any cold fuel problems in flight since it has all of the favorable items pointed out for the F-4 airplane plus the warm fuel re-circulating to the integral wing tanks.

Figure 20 shows the calculated recovery temperature for the integral wing tanks and external tanks to be -60°C at 30,000 to 35,000 feet for a light weight maximum endurance condition, and -48.5°C at 45,000 feet for a heavy weight maximum range condition. Due to the warm fuel re-circulating to the integral wing tanks, the fuel temperature could never reach these values.



9 GEN 20051

FIGURE 20. F-15 ADIABATIC WALL TEMPERATURE

OTHER AIRPLANE REVIEWS

B-52

The fuel tanks consist of 4 main tanks and 8 auxiliary tanks. The auxiliary tanks include a forward, mid, and aft fuselage tank, a left and right outboard wing tank, a center wing tank, and a left and right external underwing tank. The mains, outboard auxiliaries, and center wing tanks are all integral (wet wing) type. The fuselage tanks consist of 7 rubber cells. A schematic of this tankage system is shown in Figure 21.

The B-52 has eight engines, two in each of four nacelles.

The fuel supply system is designed so that the engines receive fuel from the nearest of the four main tanks or from the main manifold. When auxiliary fuel is being used, main tank boost pump switches are ON. The boost pumps in the auxiliary tanks are of higher pressure than the boost pumps in the main tanks thus allowing a pressure override condition to exist. When an auxiliary tank runs dry, the main tank boost pumps take over fuel supply to the engines with no interruption in fuel flow. This method of auxiliary fuel use is similar to the C-9 and is opposed to the KC-10A which transfers fuel to the main tanks while feeding the engines from the mains. Under normal operating conditions, main tank 1 supplies fuel to engines 1 and 2 in nacelle strut 1 and main tanks 2, 3, and 4 supply fuel to engines in respective nacelle struts 2, 3, and 4.

There are several methods of fuel management, depending on the number of external missiles being carried. The fuel management sequence with no external missiles is essentially as follows:

- STEP 1. (Takeoff)-----Mains To All Engines
- STEP 2. Aft Fuselage to 1, 2, 3, 4
Center Wing to 5, 6, 7, 8----- (Center Wing to Empty)
- STEP 3. Aft Fuselage to 1, 2, 3, 4
Forward Fuselage to 5, 6, 7, 8----- (Forward Fuselage to Ballast
Fuel - normally 0 to 10,000 lbs.
depending on operating weight
and number of internal missiles)
- STEP 4. Aft Fuselage to 1, 2, 3, 4----- (Aft Fuselage to 5700 lbs)
Mid Fuselage to 5, 6, 7, 8
- STEP 5. Mid Fuselage
to All Engines----- (Mid Fuselage to Empty)
- STEP 6. Aft Fuselage to 1, 2, 7, 8----- (Aft Fuselage to Empty, Mains
No. 2 Main to 3 and 4 2 & 3 to 38,700 lbs. Each)
No. 3 Main to 5 and 6
- STEP 7. Mains to All Engines

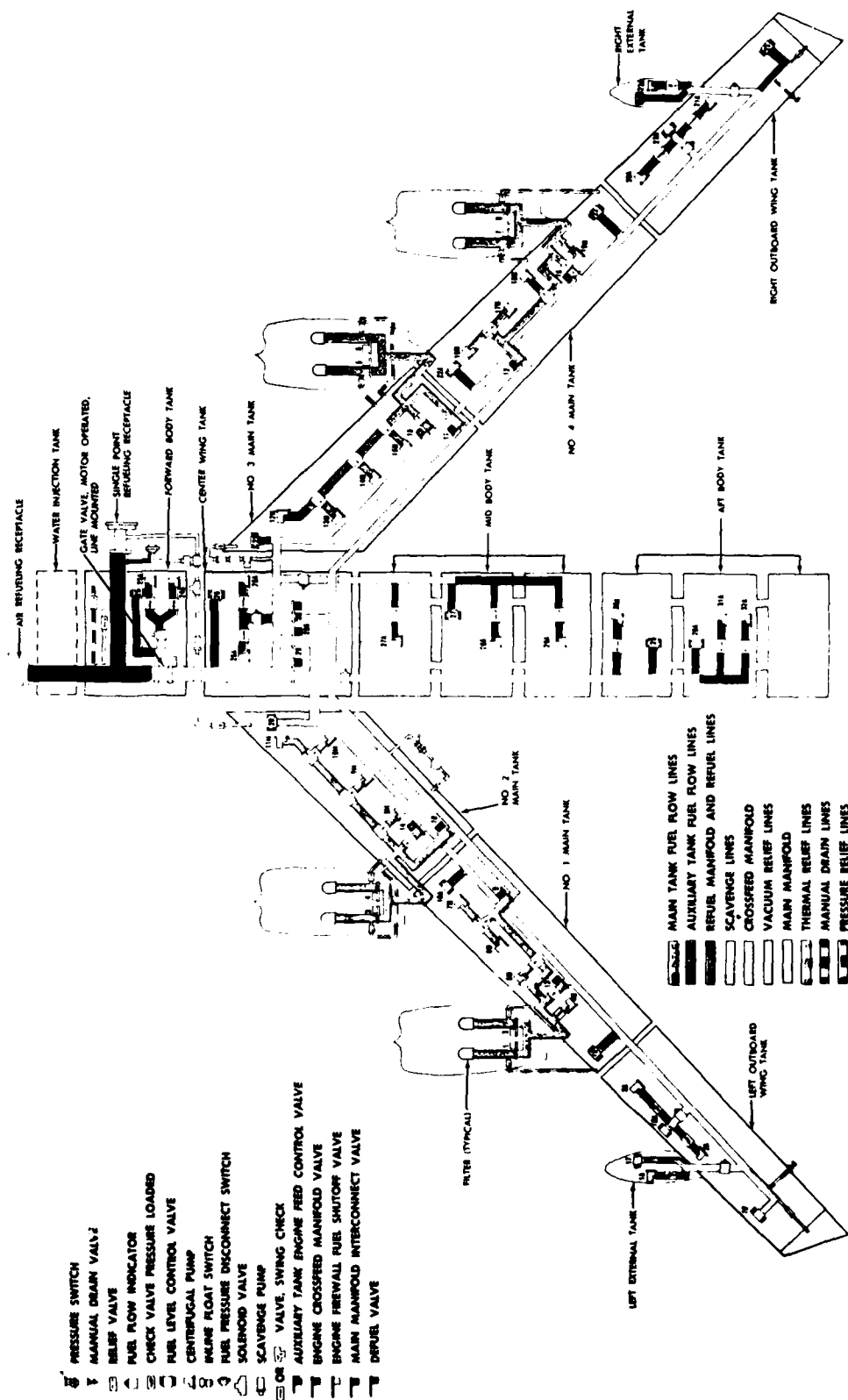


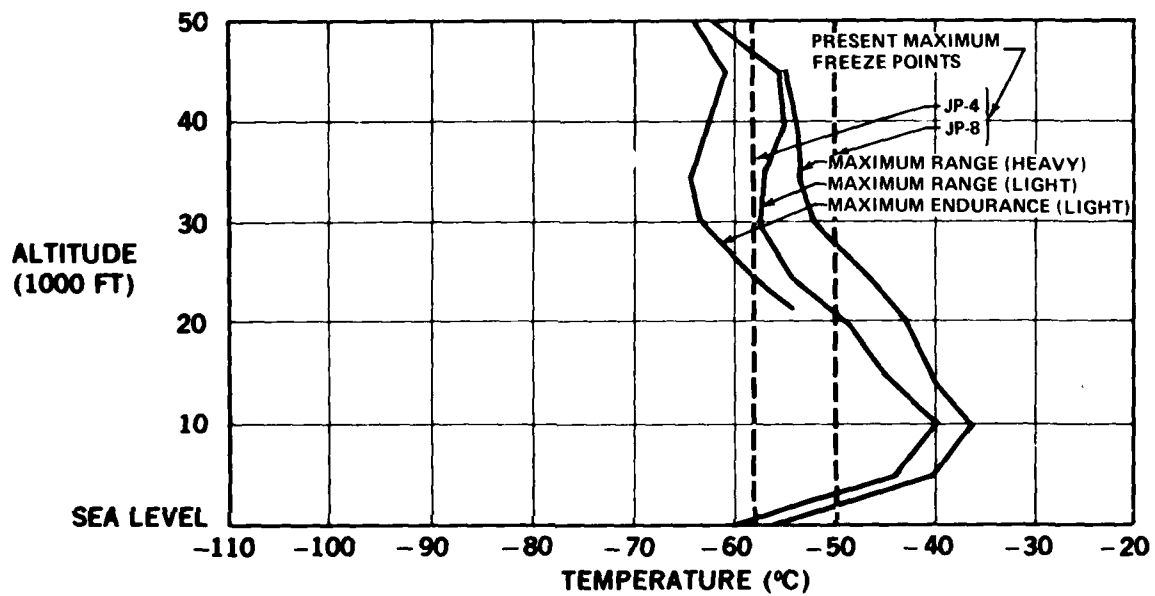
FIGURE 21. B-52 FUEL SYSTEM

- STEP 8. Left Outboard to 1 & 2----- (Outboards to Empty, Mains
 No. 2 Main to 3 & 4 to 9,000 lbs. Each)
 No. 3 Main to 5 & 6
 Right Outboard to 7 & 8
- STEP 9. Mains to All Engines----- (All Mains to 5,000 lbs.
 Each)
- STEP 10. Left External to 1, 2, 3, 4,----- (Externals to Empty)
 Right External to 5, 6, 7, 8
- STEP 11. Forward Fuselage to----- (Forward Fuselage to Empty)
 All Engines
- STEP 12. Mains to All Engines

It can be seen that this schedule uses the most thermally insulated fuel first (center wing and fuselage) and the coldest fuel last (outboards and external). The engines are fed directly from the auxiliary tanks, as opposed to the KC-10A which transfers all auxiliary fuel to the mains and therefore tends to keep the main tank fuel warmer. Keeping the main tank fuel warmer is always desirable because this is the fuel that feeds the engines during the last portion of the flight. The external tanks of the B-52 contain 4550 lbs of fuel each. This external fuel will reach the recovery temperature early in the flight and there after will essentially follow the recovery temperature within a few degrees. The external fuel is fed to the engines by tank mounted boost pumps. The problem of congealed fuel being "hung up" in portions of the tank and not flowing to the pump inlet should not be as severe in these external tanks, as compared to integral tanks, due to their shape, type of construction, and greater relative tank heads.

Figure 22 shows the calculated recovery temperatures for the integral wing tanks and external tanks to be -64°C at 50,000 feet for a light weight maximum endurance condition, and -55°C at 45,000 feet for a heavy weight maximum range condition.

The B-52 Flight Manual states to avoid flying at altitudes where the indicated total air temperature is below the freeze point of the fuel. It can be seen that the above recovery temperatures are well below the maximum specified fuel freeze point temperature of JP-8 and JP-4 (-50°C and -58°C respectively). Therefore, there is a possibility of the fuel temperature reaching the freeze point if operation is continued in this environment for a long enough period of time. If the specified maximum freeze point were increased. There would be a greater chance for the fuel temperature to drop below its freeze point. The only way to find out how serious the problem could be is to conduct an extensive flight test program on this or a similar type aircraft, in conjunction with knowing the exact type of missions that the aircraft will perform.



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FIGURE 22. B-52 ADIABATIC WALL TEMPERATURE

C-130

The fuel tank system consists of four main integral wing tanks (one for each engine), two bladder cell type auxiliary wing tanks, and two additional welded metal pylon mounted external tanks. The two internal auxiliary tanks are located on each side of the dry center section bay. This tankage arrangement is shown in Figure 23. To extend the operating range, four metal fuselage tanks with a capacity of 500 gallons each can be installed in the cargo compartment. All of the auxiliary fuel is used first by transferring to the No. 2 and 3 Main Tanks. The engines are then fed from the No. 2 and 3 Main by means of cross-feed valves with the No. 1 and 4 Main Tank boost pumps turned off. The pylon and auxiliary wing fuel are used first, followed by the fuselage tank fuel (if incorporated). The engines are then fed from their respective main tank for the remainder of the flight. A schematic of the fuel system is shown in Figure 24.

This schedule is similar to the KC-10 in that it leaves the outboard wing fuel until the later portion of the flight. However, in the case of the KC-10, the relatively warmer auxiliary fuel is pumped through the tip tank during transfer. In the case of the C-130, the No. 1 and 4 Main (outboard tanks) fuel is isolated with no agitation from the boost pumps for the total time that it takes to transfer all of the auxiliary fuel. Since this tank has a relatively large cooling surface to tank volume, this fuel has the potential of being very cold. However, this fuel does not have to be transferred inboard to feed the engines as in the case of the KC-10. The fuel is pumped directly to the engines with the boost pumps which should present fewer problems. If all of the No. 1 and 4 Main Tank fuel is required to complete the flight, the fuel at the tank surface should not be allowed to congeal. With the relatively low cruise Mach numbers of the C-130, this airplane may control the maximum fuel freeze point for all of the airplanes in the study unless segregated fuel supplies can be established.

Figure 25 shows the equilibrium recovery temperatures for the C-130 to be -69.5°C at 35,000 feet for a light weight maximum endurance condition, and -63°C at 30,000 feet for a heavy weight maximum range condition.

The C-130 Flight Manual instructions say to consider the fuel temperature to be equal to the indicated total air temperature. It also states to not allow the fuel temperature to be lower than 6°F (3.3°C) above the fuel freeze point. The above recovery temperatures are well below this limit. The flight manual also specifies that Jet A-1 fuel (same freeze point as JP-8) should not be used if operation in temperatures below -40°F (-40°C) is anticipated.

These possible fuel temperatures (especially for the case of the No. 1 and 4 main tanks) and operating instructions preclude the possibility of increasing the freeze point of JP-4 or JP-8 without flight tests in extreme cold environments with instrumented tank fuel temperatures.

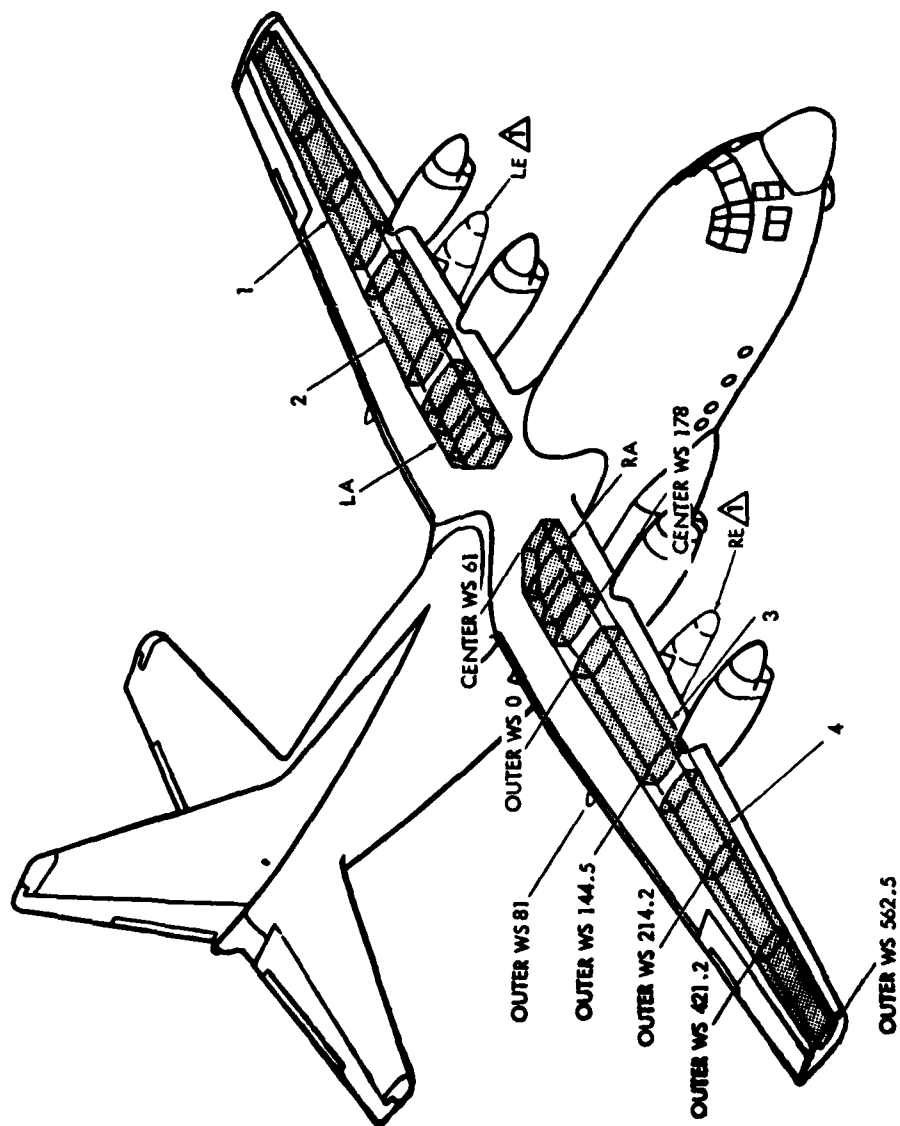


FIGURE 23. C-130 TANKAGE ARRANGEMENT

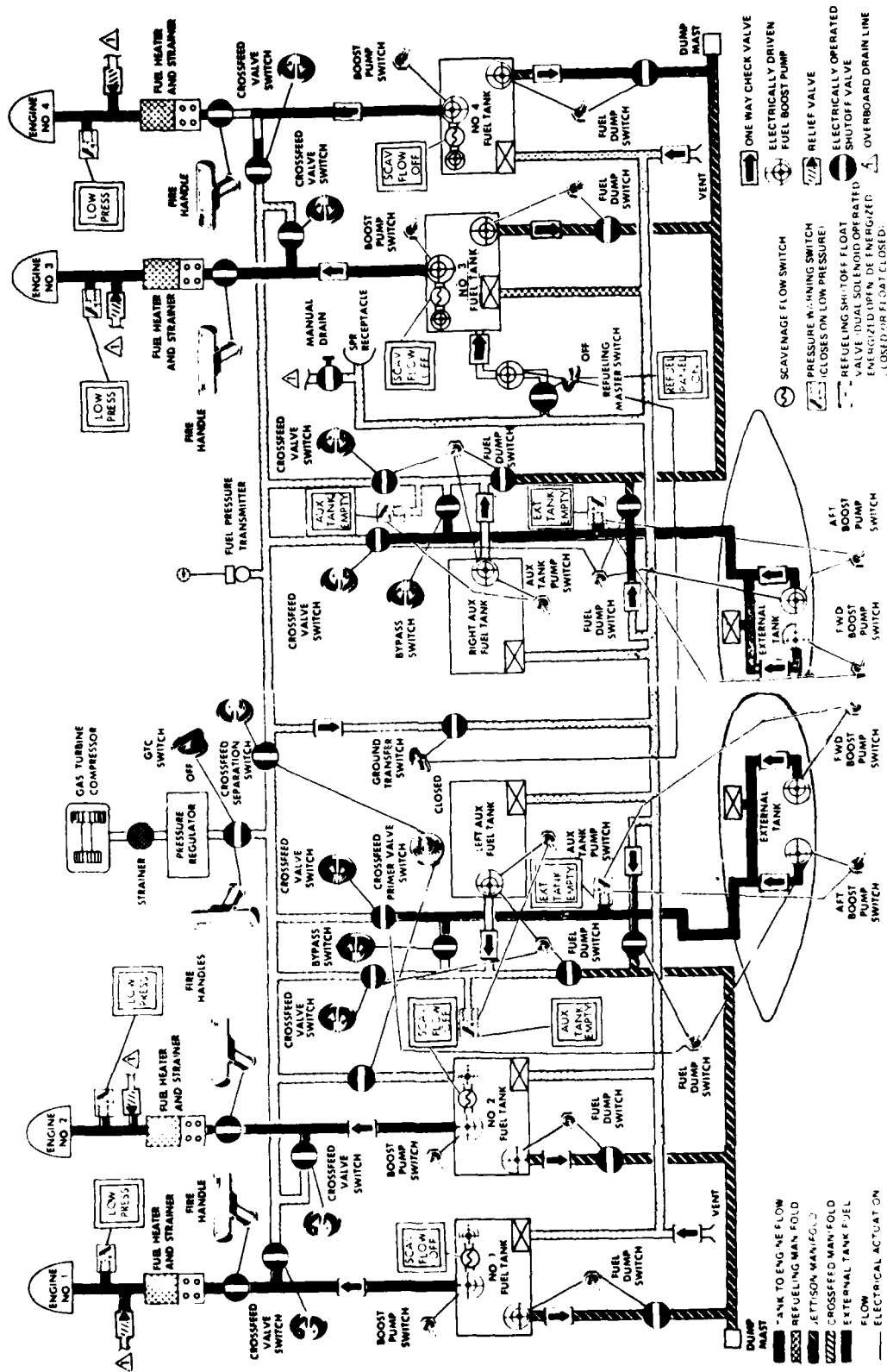
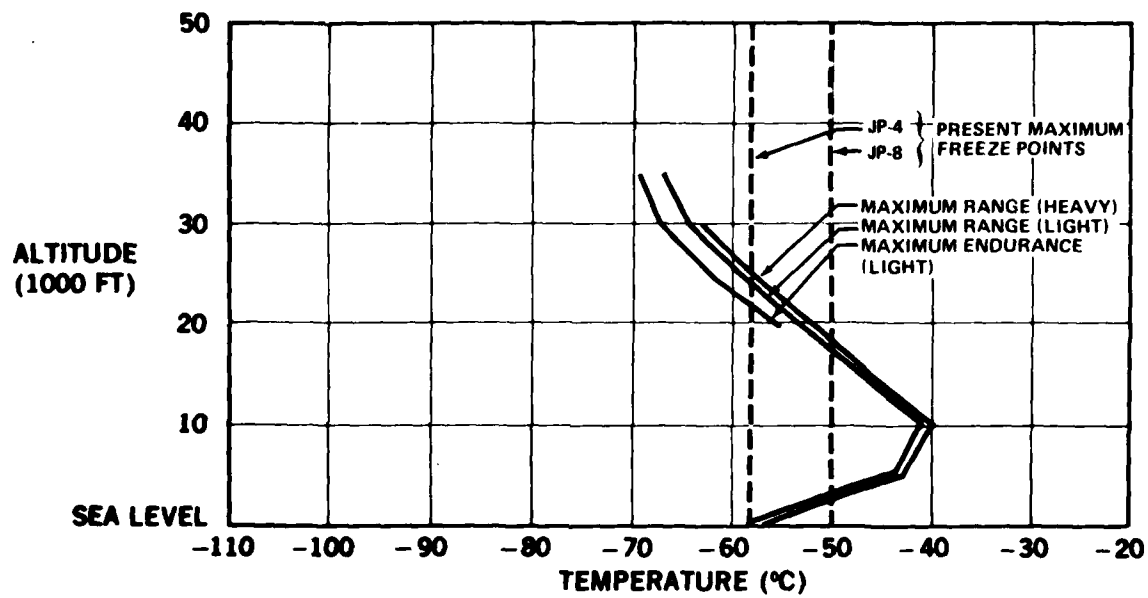


FIGURE 24. C-130 FUEL SYSTEM



9 GEN 78649

FIGURE 26. C-130 ADIABATIC WALL TEMPERATURE

KC-135

Fuel is contained in four main tanks, a center tank, and three fuselage tanks. The four main tanks are integral types. The No. 1 and 4 main tanks include an outboard reserve tank. The fuselage tanks and the center portion of the center wing tank consist of bladder cells that are contained within structural cavities. The outer portion of the center wing tank is an integral type. The tankage arrangement is shown in Figure 26, and a schematic of the fuel system is shown in Figure 27.

The fuselage tanks and center wing tank fuel (if used to feed the engines) are used first and fed directly to the engines by means of pump pressure that overrides the main tank boost pump pressure. The engines are then fed from the outboard mains (Nos. 1 and 4) until the outboard main plus the outboard reserve fuel equals the inboard (Nos. 2 and 3) main fuel. The outboard reserve fuel is then allowed to transfer inboard by gravity and the engines are then fed from their respective main tank for the remainder of the flight.

This fuel management system has a fuel schedule similar to that of the B-52 and the KC-10A to save airframe weight (use of wing tip ballast fuel). It has the pump pressure over-ride system of the B-52 which does not allow the relatively warm auxiliary fuel to mix with the colder main tank fuel. It also has the reserve tank fuel which must be retained until near the end of the flight similar to the KC-10A. The reserve tank fuel on the KC-135 is not disturbed from take-off until transfer is signaled. There is no mixing action which would prevent temperature stratification. This temperature stratification would reduce heat transfer from the bulk fuel, however it would tend to build up a congealed layer of fuel at the tank surface and may form a matrix of wax that may trap liquid fuel. As in the case of the KC-10A, this reserve fuel cannot be allowed to become so cold that it congeals near the lower surface of the tank if a timely inboard transfer of fuel is to be expected on demand.

The recovery temperatures for the KC-135 are shown in Figure 28, and are -63.5°C at 35,000 feet for a light weight maximum endurance condition and -54°C at 40,000 feet for a heavy weight maximum range condition.

The KC-135 Flight Manual instructions state to avoid flying at altitudes where the indicated total air temperature is below the freeze point of the fuel. The recovery temperatures shown in Figure 28, are fairly realistic for this aircraft since it may be called upon to conduct a loiter type of tanker mission and "hold" in a cold air mass at a low Mach number in preparation for a receiver rendezvous.

The KC-135 fuel system is very similar to the B-707. The E4 (B-747) uses a similar reserve tank and transfer method. Current studies sponsored by USAF and NASA will provide information on low temperature fuel use considerations for this aircraft.

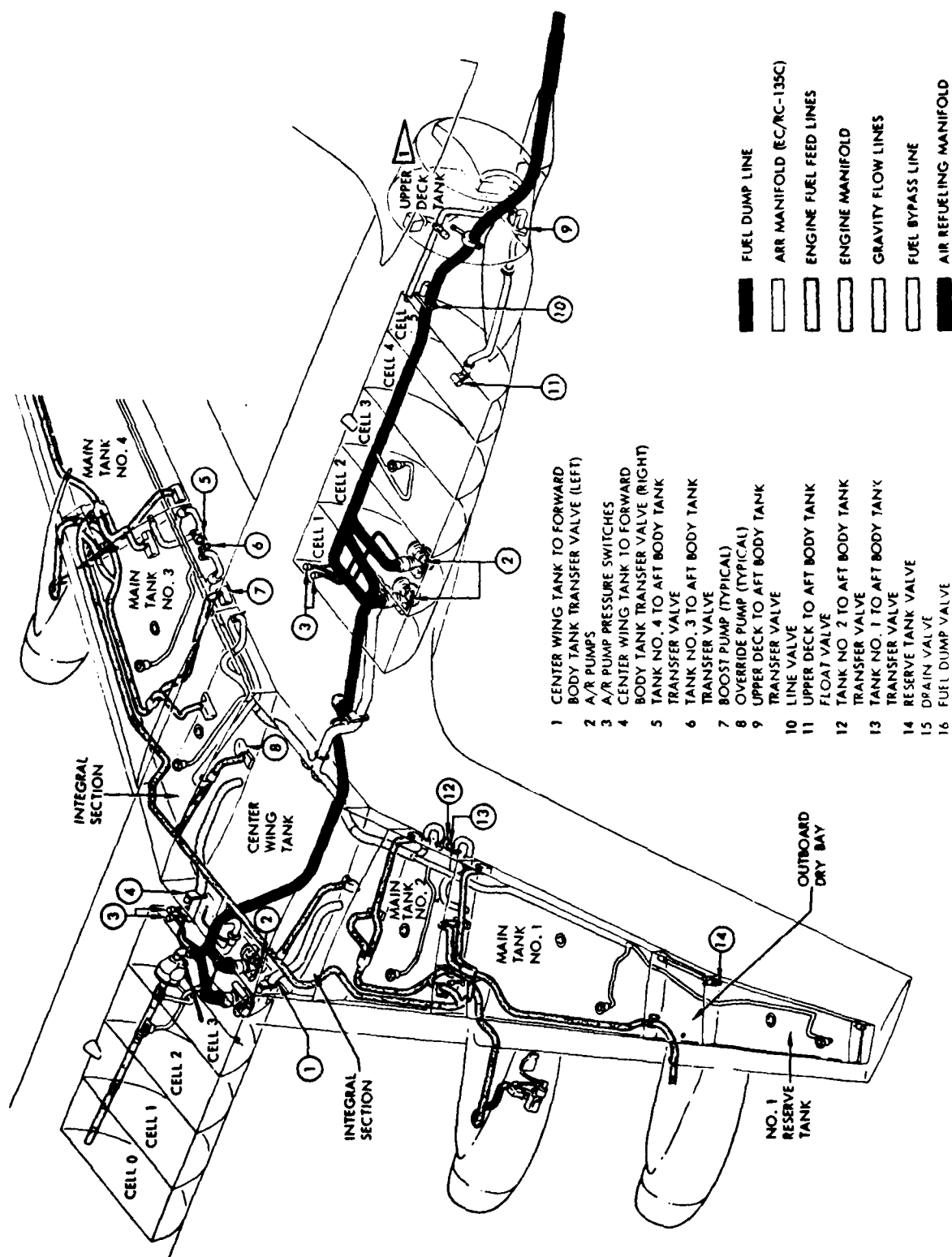
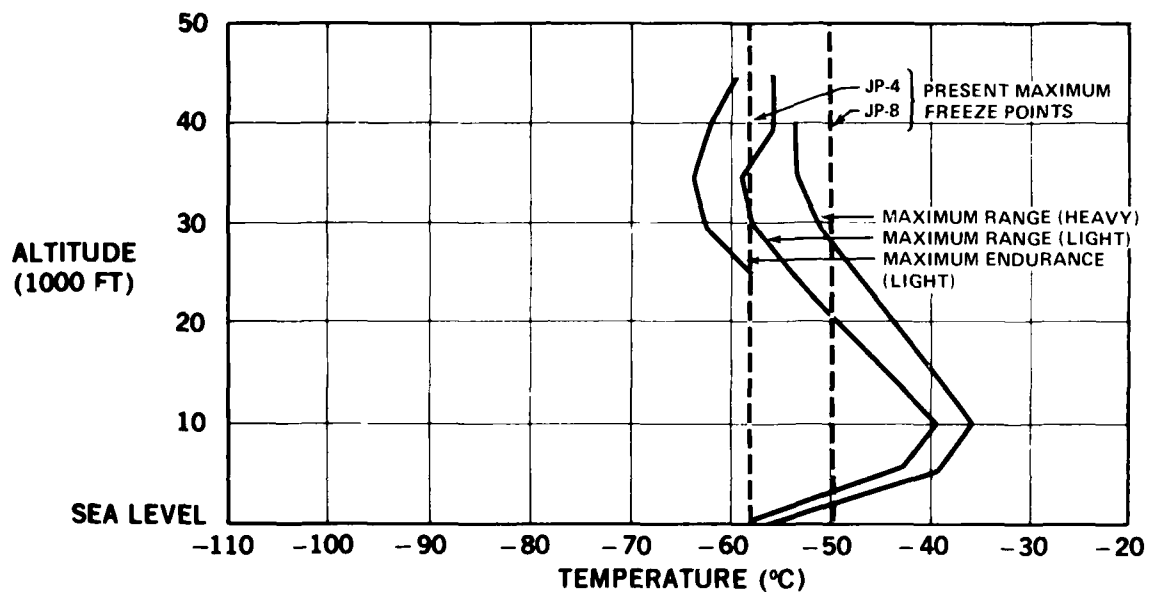


FIGURE 26. KC-135 TANKAGE ARRANGEMENT



9 GEN 28655

FIGURE 28. KC-135 ADIABATIC WALL TEMPERATURE

C-141

The fuel supply system consists of ten integral wing tanks. They include four main tanks, four auxiliary tanks, and two extended range tanks. This system is shown in schematic form in Figure 29. The extended range and auxiliary fuel is used first and fed directly to the engines by means of pump pressure that overrides the main tank boost pump pressure. All of the tanks contain jet pump scavenge systems to reduce the amount of unavailable fuel.

This system feeds the auxiliary fuel directly to the engines rather than passing the warmer fuel through the mains on the way to the engines. This method is similar to the C-9, B-52, and KC-135. The jet pumps located in the mains of the C-141 will tend to prevent temperature stratification and thus increase heat transfer from the fuel. This would have the opposite effects as those described for the reserve tanks of the KC-135 which have no agitation. The coldest fuel will be in the extreme outboard tanks. These outboard tanks are the No. 1 and 4 mains on the C-141 and incorporate boost pumps, jet pumps and collector tanks thereby eliminating any inboard gravity flow problems as in the case of the KC-135 and KC-10A.

Figure 30 shows the calculated recovery temperature for the C-141 to be -68.5°C at 35,000 feet for a light weight endurance condition and -55°C at 35,000 feet for a heavy weight maximum range condition. The Flight Manual instructions say to avoid flying at altitudes where the indicated fuel temperature is lower than 6°C above the freeze point of the fuel. The fuel temperature is measured approximately mid-way between the tank and the engine. Although the C-141 is a transport aircraft and doesn't fly low Mach number endurance/ loiter missions, it is still difficult to justify an increase of the specified maximum allowable fuel freeze points without sufficient flight test data to support such a decision.

C-5

The fuel supply system consists basically of twelve integral wing tanks served by a multi-purpose manifold which is segmented by separation valves to provide an independent supply of fuel for each engine. The tanks include four main tanks, four auxiliary tanks, and four extended range tanks. The four main tanks provide direct tank-to-engine fuel, while the auxiliary and extended range tank fuel is first transferred to the main tanks before being pumped to the engines. A schematic of the fuel system is shown in Figures 31 and 32.

The Number 1 and 4 Main Tanks are the most outboard tanks on the airplane. The C-5 fuel system has several favorable features with respect to alleviating cold fuel problems. The auxiliary fuel transfers to the main resulting in a mixing action plus agitation of the fuel. The wing bending relief is taken out by holding fuel in the tips as is in the case for most large airplanes, however, in this case, the outboard fuel is a main tank. No inboard transfer near the end of the flight is required. Also, the transfer of the extended range fuel and auxiliary fuel to this outboard main will tend to keep the temperatures from dropping as fast as in isolated tanks.

Figure 33 shows the calculated recovery temperature to be -65°C at 35,000 feet for the light weight endurance condition and -53.5°C at 37,000 feet for the heavy weight maximum range condition. The C-5 Flight Manual instructions say to avoid flying for more than 30 minutes at altitudes where indicated total air temperature

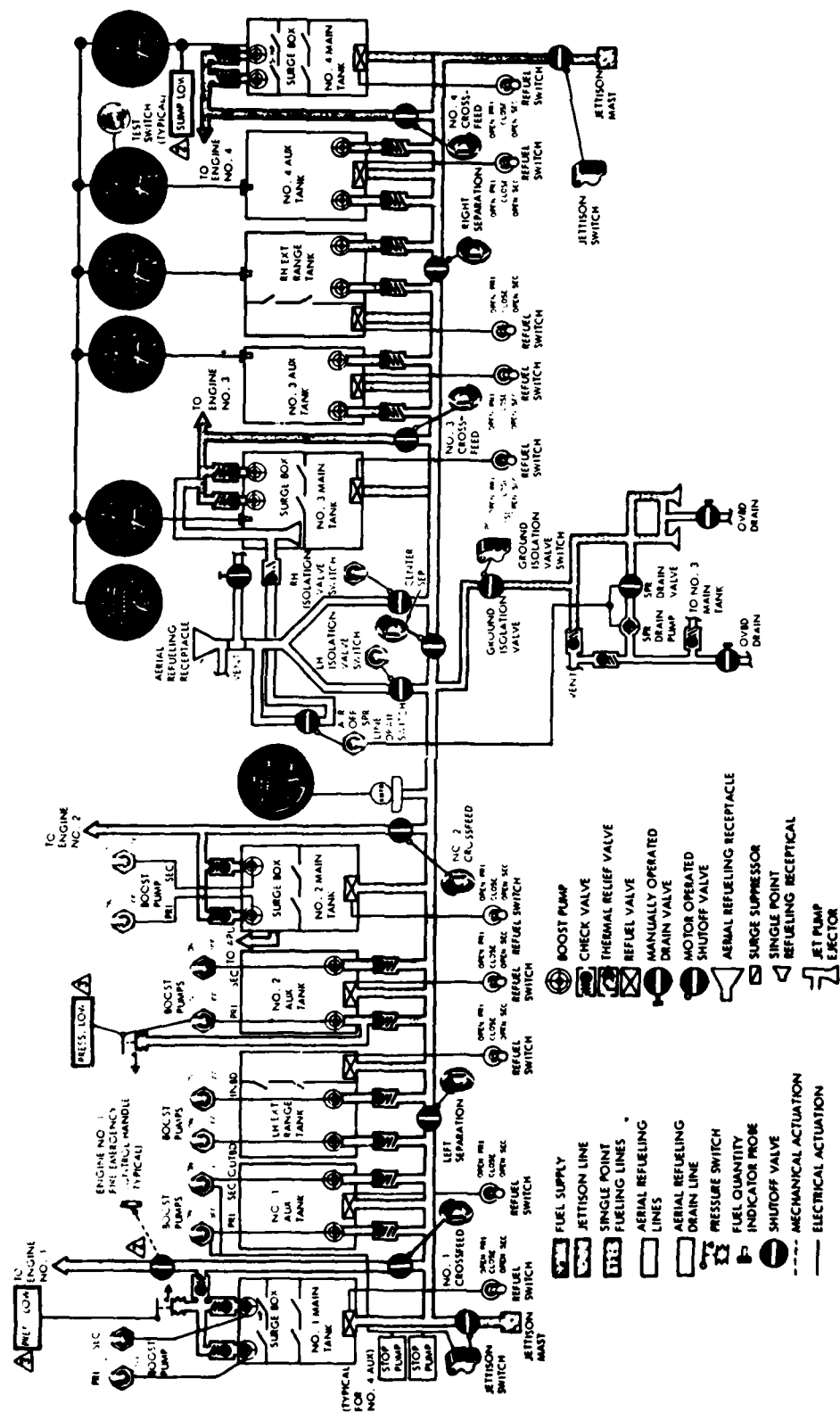
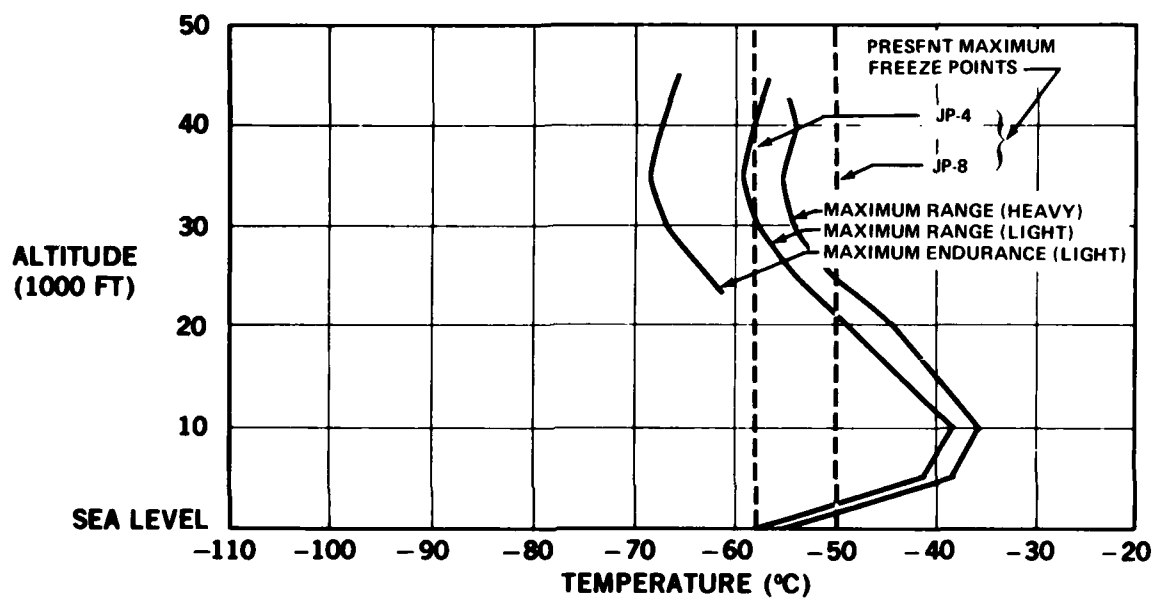


FIGURE 28. C-141 FUEL SYSTEM



9 GEN 28654

FIGURE 30. C-141 ADIABATIC WALL TEMPERATURE

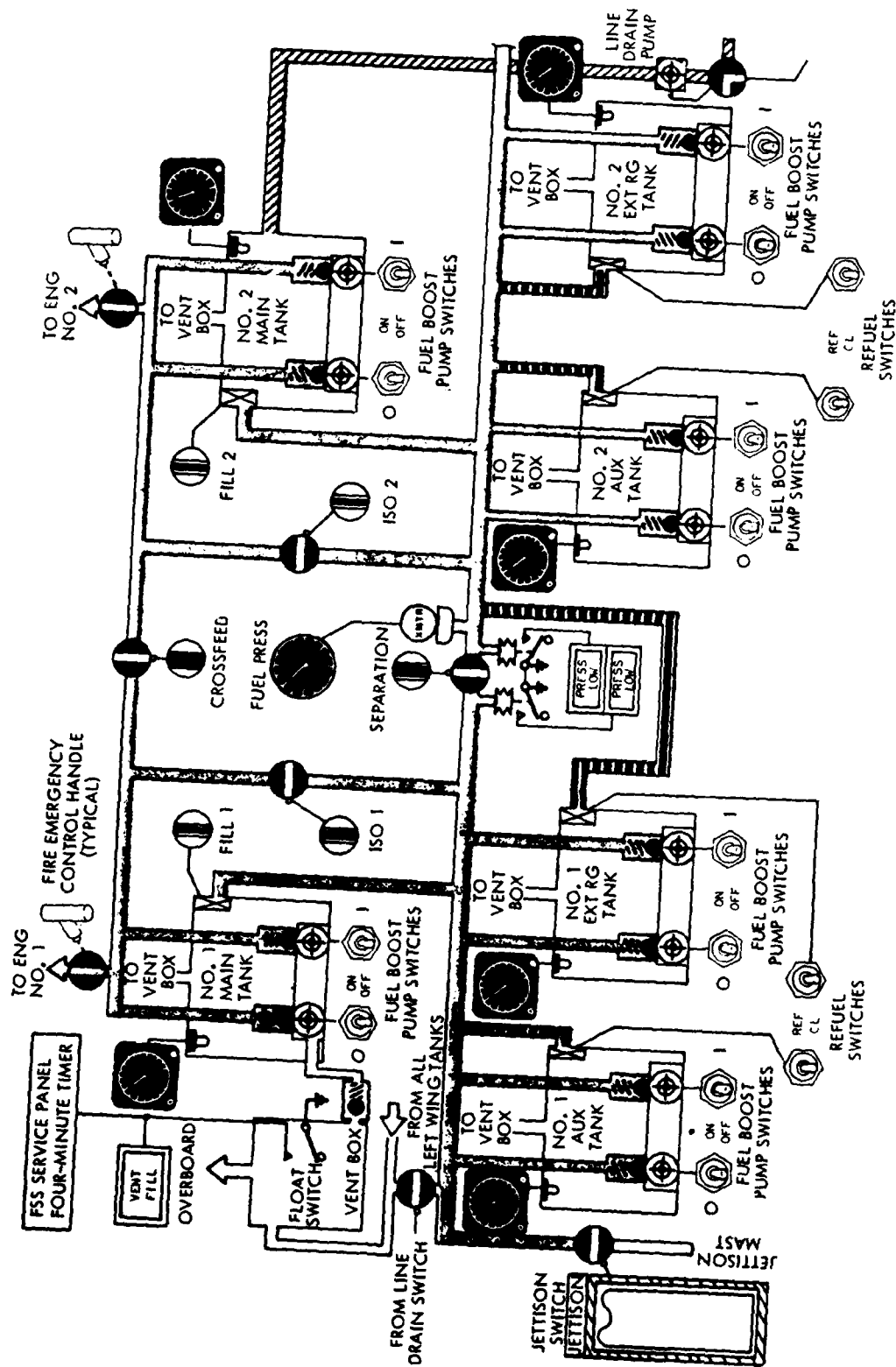


FIGURE 31. C-5 FUEL SYSTEM (LEFT SIDE)

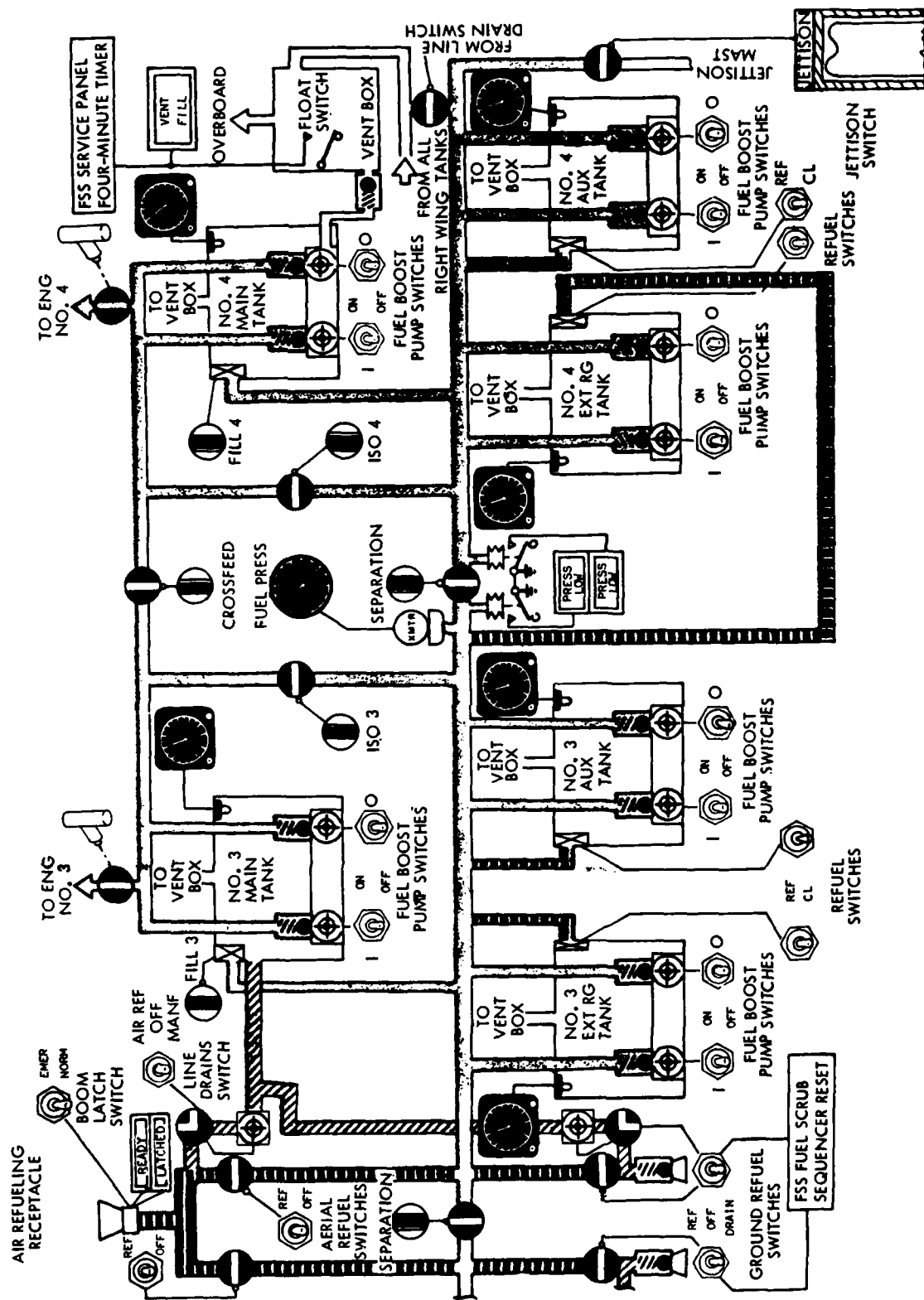
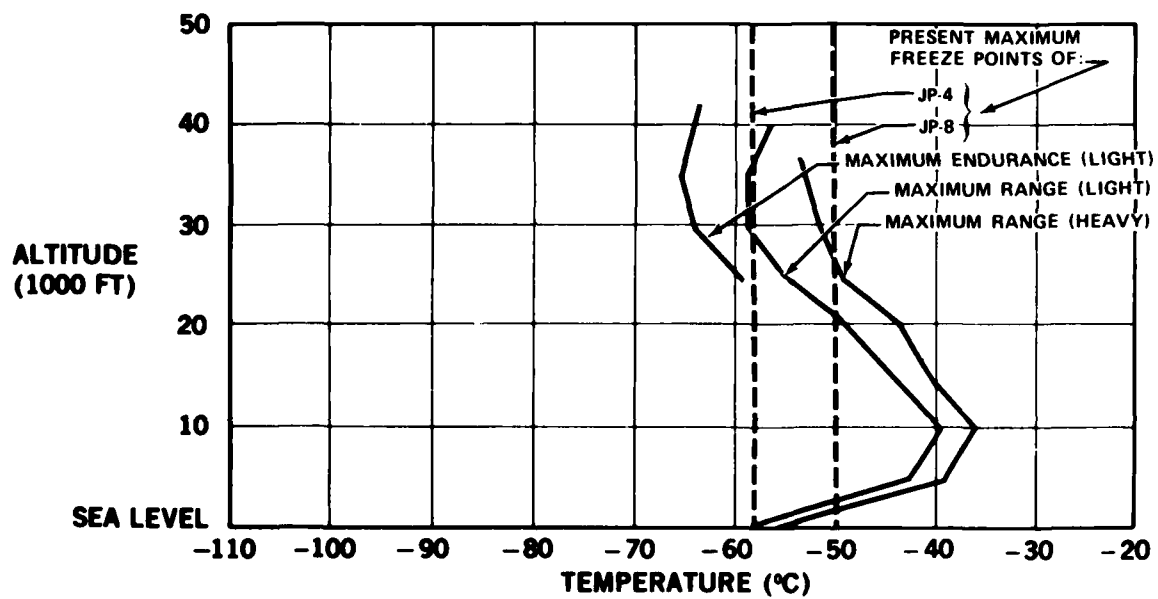


FIGURE 32. C-5 FUEL SYSTEM (RIGHT SIDE)



9 GEN 28645

FIGURE 33. C-5 ADIABATIC WALL TEMPERATURE

is below the freeze point of the fuel plus 3°C. Since the C-5 does not fly loiter type missions, the fuel is less likely to reach these extreme cold temperatures. Although the fuel system design and management methods indicate that the C-5 should probably be able to operate with fuel temperatures slightly closer to the freeze point than the other transport type aircraft in this study, it is difficult to determine the degree without conclusive flight test data.

DISCUSSION OF CONGEALED FUEL BUILDUP

For each model aircraft there is obviously some amount of congealed fuel which could be allowed before the critical flow of fuel to the boost pumps would be unacceptably impaired. There will be varying amounts of congealing experienced in different locations as the fuel cools due to the differences in structural configurations, e.g., the amount of clipping involved (shear clips between skin & rib), and the fuel mass available to supply heat.

There are no clear criteria which may be established and confidently evaluated as a limiting condition on adequate tank internal fuel flow. At each location in the fuel tank the amount of fuel head available, the rate of flow required, the rate of change of fuel head and of fuel flow rates, the effect of wetted surface area to locally affected fuel volume are all significant factors in the transient analysis of tank fuel behavior. An insufficient data base exists to adequately correlate all these items, along with the other factors of fuel properties, fuel constituency, aircraft mission variations, and all of the considerations of failure conditions as would be required for a determination of how much fuel congealing could be allowed.

SURVIVABILITY/VULNERABILITY

The proposed fuel property changes were reviewed with personnel knowledgeable in the mechanism and reactions of concepts used for self-sealing lines and tanks. No change in the capability of these special features to protect flight crews and vehicles are expected as a result of the proposed fuel property changes.

In specific regard to survivability aspects of operations in a combat environment, the proposed property changes will have little or no effect on vapor pressure and are directionally toward reduced volatility. Any effect in this area will be so small as to be unidentifiable, but is directionally toward improved survivability.

The effect of fuel property changes on infrared signature or smoke levels are so small as to be insignificant. Most of the increased metal temperatures occur in areas which are not visible during normal operations.

FUEL PROPERTY VARIATION LIMIT SELECTION

Limits on the maximum allowable fuel freeze points for JP-4 and JP-8 fuel supplied to the USAF have been investigated by examining the adiabatic wall temperature on selected aircraft wing tanks during various flight conditions and by considering other parameters which would affect freeze point selection. A MIL-STD-2108 one day per year risk minimum outside air temperature was assumed. The types of missions flown by each aircraft which place it in these flight conditions were considered. Fuel system and fuel usage schedules were considered in some detail.

It was believed that wing adiabatic wall temperature alone as a limit would be overly conservative. However, there is as yet no correlation among fuel properties, wall temperature, time, geometry, fuel schedule, fuel system effects, and outside air temperature variations -- all of which affect successful recovery of a satisfactory quantity of fuel from the tanks as required -- which would allow use of a value higher than the adiabatic wall temperature.

The values in Table I show the allowable change in fuel freeze point for each type of aircraft considered. These values were calculated by subtracting the adiabatic wall temperature for the airplane and the flight condition from the current values of maximum allowable freeze point. The significant operating condition for each aircraft is underlined. The tanker aircraft would be subject to light loiter conditions, the transport missions would at worst be at maximum range conditions and lightly loaded toward the end of cruise, and the fighter aircraft would only have wing fuel or external fuel under relatively heavy conditions.

This chart shows that no change in maximum allowable fuel freeze point can be allowed for all USAF fuel supplies. Indeed, many aircraft can experience fuel temperatures below current allowable freeze point limits, if operating for a sufficient length of time within a MIL-STD-2108 temperature environment.

The chart values could be interpreted to suggest that current operations with fuel procured to current maximum allowable fuel freeze point levels may be inadequate. This is not the case. Currently, fuel is almost always supplied with fuel freeze points below the maximum allowable by specification. JP-4 fuels usually have actual freeze points very much below the specified maximum allowable. The actual frequency of encountering the very low temperatures listed in MIL-STD-2108 is unknown, but may very well be below the frequency shown there. In order to have a problem on an aircraft, the low air temperatures must be encountered for a significant period of time at the same time fuel of high freeze point is being used. Data on the combined probability of these occurrences, if available, has not been treated in this study, but the lack of experience with fuel freeze point problems indicates that probability is very low. As actual fuel freeze points are increased the probability of exposure to potential problems increases. Increases in allowable freeze points will increase that probability.

The chart values could also be interpreted to suggest that the scope of the current study is inadequate to thoroughly define the situation. This is very much the case.

If any of the airplanes in service encountered conditions which caused the fuel temperature to even approach the freeze point, the pilot would have to take evasive action (increase Mach number and/or decrease altitude) if he complies with the aircraft flight manual. The "dash one" Technical Order (the commercial airplane flight manual equivalent) for all of the airplanes in this study instructs the pilot either to not fly in conditions where the ram air temperature is at or below

- Notes:
1. Outside air temperatures from MIL-STD-210B, one day per year risk minimum temperatures.
 2. Based on adiabatic wall temperatures.

JP-8

	<u>KC-10A</u>	<u>C-9</u>	<u>B-52</u>	<u>C-130</u>	<u>C-135</u>	<u>C-141</u>	<u>C-5</u>	<u>F-4</u>	<u>F-15</u>
Max range - heavy	0.5	-3.0	-5.0	-13.0	-4.0	-5.0	-3.5	4.5	1.5
Max range - light	-3.5	-6.5	-11.5	-16.5	-8.5	-9.5	-8.5	-0.5	-4.0
Max endurance - light	-10.5	-14.5	-14.0	-19.5	-13.5	-18.5	-15.0	-7.5	-10.0

JP-4

Max range - heavy	8.5	5.0	3.0	-5.0	4.0	3.0	4.5	12.5	9.5
Max range - light	4.5	1.5	-3.5	-8.5	-0.5	-1.5	-0.5	7.5	4.0
Max endurance - light	-2.5	-6.5	-6.0	-11.5	-5.5	-10.5	-7.0	0.5	-2.0

Table I. Allowable Freeze Point Increase (°C).

the freeze point of the fuel on board, or to not allow the fuel temperature to go below a given amount above the freeze point of the fuel on board. If it was proposed to operate with fuel temperatures below those specified in the manuals, the aircraft manufacturer would have to determine new fuel freeze point margins (fuel temperature minus fuel freeze point). Douglas has been requested several times in the recent past to decrease this margin by customers for our commercial airplanes. The result of the investigation was that the margin could not be reduced without increasing the accuracy of the fuel temperature indicating system components. Even with the increased accuracy system the margin could only be decreased from 5°C to 3°C in some cases.

The fuel temperature indicating system sensor is usually located in tank locations that normally contain the coldest fuel on board the airplane. It is recognized that airplanes don't usually operate in a severe environment for the entire flight. If the outside air temperature increases during the later portions of the flight, the fuel at the location of the temperature sensor is usually the first to increase in temperature. However, to allow the indicated fuel temperature to go below the fuel freeze point and assume that it will warm up later in the flight would require an extensive analysis of this operation and a definition of limits.

There have been test programs in the past to study the "pumpability" or "flowability" of fuel in aircraft tanks at temperatures near the freeze point or pour point. A recently completed USAF contract test program suggested that if the fuel at a location one inch above the lower skin of a test tank was maintained at or above the fuel freeze point, few problems were encountered in pumping the fuel from the tank. The data and the significance of results from this program are not yet published. A comprehensive program would be required to correlate an aircraft production fuel temperature indicating system with the fuel temperature one inch off the bottom of the tank.

A study to determine allowable freeze point increases on a geographical, seasonal, or airplane type basis could possibly offer some fuel cost/availability relief to the USAF, but would result in a multi-grade fuel supply requirement to service transient aircraft. The cost savings to make such a system financially feasible would have to be very much more than was indicated in preliminary reports by the fuel supplier as part of this contract.

SUMMARY AND CONCLUSIONS

The objective of Phase I was to determine the amount of freeze point increase of JP-4 and JP-8 that can be tolerated without appreciably degrading aircraft system performance, safety, or survivability. First, it was determined which airplanes of the Air Force inventory were using the majority of fuel supplies from 1979 through 1985. Next, the MIL-STD-210B one-day-per year risk minimum temperature was chosen as a reasonable minimum ambient temperature envelope for USAF aircraft operations. All of the airplanes in the study were placed within this temperature envelope during "fuel temperature critical" type missions. The aircraft adiabatic surface temperature (recovery temperature) was then calculated for each mission. The adiabatic surface temperature on the significant missions was selected as the limiting case for fuel freeze point considerations.

The specification maximum allowable freeze point of JP-4 or JP-8 cannot be increased without degrading system performance and safety as critical conditions are approached.

The environmental envelope for aircraft operations is the most important overall factor in appraisals of the adequacy of fuel freeze points. A realistic envelope requirement is needed.

MIL-STD-210B one day per year risk temperature levels compare favorably with reported data and other studies on upper air temperature, but has limited applicability.

A specific aircraft's peculiarities of operating envelope, geometry, and system response at low temperatures are very important in evaluating how the aircraft will function under conditions where actual fuel freeze point temperatures are to be approached.

The ability to operate current aircraft closer to actual fuel freeze points is constrained by operating limits applied to specific aircraft by the manufacturers. These limits will have to be reconsidered by the manufacturers, who are unlikely to have sufficient data available to extend the limits.

The design of future large aircraft is leading to a greater probability of limit encounters if fuel freeze points are raised.

A geographically localized fuel freeze point increase may be allowed where aircraft, missions and the flight environment permit. The determination of where and by how much such increases are permissible is beyond the scope of this investigation.

The USAF may increase fuel freeze points in peace time by being willing to examine flight profiles, actual fuel freeze points, and upper air temperatures on critical flights prior to dispatch and by planning for diversions or flight cancellations as a method of coping with low fuel temperatures.

A war time position of perpetual readiness to go anywhere, anytime, with any equipment, will require the USAF to standardize on low freeze point fuels.

Some aircraft are not adequately equipped to give the crew fuel temperature information essential to operations where actual fuel freeze point temperatures are to be approached.

The proposed increases in freeze point, smoke point and end point will have no effect on survivability/vulnerability.

The relaxed specifications for JP-4 and JP-8 fuels are expected to have an insignificant effect on the materials used in self sealing fuel lines and fuel tanks.

RECOMMENDATIONS

An extensive effort should be made to determine a realistic design and operating temperature envelope that considers time of flight and geographical flight path as independent variables. This will release the USAF from any constraints imposed by the current data base that have resulted in MIL-STD-210B where time and flight paths were not considered. A program of temperature recordings should be started immediately to get a data base.

A survey of aircraft at specific bases and their missions should be made to evaluate the ability to convert the base fuel supply to higher freeze point fuels.

The USAF should consider the use of actual freeze points rather than specification maximums for aircraft dispatch evaluations on non-critical missions, to ease the impact of any move to higher allowable freeze points. A specific fuel sampling procedure will be required for each aircraft. A rapid plane-side method for determining actual fuel freeze points should be developed for use with mixed fuel loads or where fuel supply data is not available.

All aircraft in the inventory which are to be operated close to fuel freeze points should have a review of their systems & procedures for operating nearer to actual fuel freeze points.

A test procedure guideline and a test fuel should be developed to enable manufacturers to run meaningful tests for evaluation of their systems and the development of instrumentation system improvements.

Future aircraft design requirements should recognize the desire to make systems less sensitive to low temperature operations. Fuel tankage and fuel management systems of future aircraft should be evaluated for the trade offs between designing to handle higher freeze point fuels and any fuel cost savings to be realized.

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